

Fluid-Structure Interaction Simulations Of Supersonic Parachute Inflation: Model Sensitivities

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Fluid-structure interaction simulations have the potential to accelerate the certification process of parachute systems for interplanetary robotic exploration missions while helping to mitigate risks by covering a wider range of scenarios than can be flight tested. The Launch, Ascent, and Vehicle Aerodynamics team is working toward actualizing this potential by further developing the capability to perform such fluid-structure interaction simulations and validating it with the best supersonic parachute flight test data available. We perform several simulations of the Advanced Supersonic Parachute Inflation Research Experiments’ third flight test (SR03) where we independently vary several parameters to determine their sensitivity on the predicted drag. We investigate sensitivity of the drag to the added mass of the confluence fitting, to the effective porosity, to how the gore seams are modeled, to viscous effects, and to deceleration. Modeling the added mass of the confluence fitting has virtually no impact on the drag, but is worthwhile to include to better match the overall system’s mass, which is important for the simulations with deceleration. Parachute drag is only marginally sensitive to porosity and viscous effects. Maximum parachute drag is most sensitive to how the gore seams are modeled, causing a nearly 20% drop when they are modeled as stiffer radials, and to deceleration, causing an additional 10% reduction. Results show improved agreement with flight test measurements when the FSI model is closest to the as-built configuration and physics experienced in flight: with the confluence fitting, viscous effects, the gore seams modeled as stiff radials, and including deceleration.

I. Nomenclature

M_∞	=	freestream Mach number
q_∞	=	freestream dynamic pressure (Pa)
ρ_∞	=	freestream density (kg/m ³)
α	=	freestream angle of attack (°)
D_0	=	parachute nominal diameter (m)
S_0	=	parachute nominal cross-sectional area (m ²)
C_A	=	axial force coefficient (i.e. in the x direction)
c_e	=	effective porosity
Δy^+	=	wall-normal mesh spacing in viscous units
F_{aero}	=	integrated aerodynamic force (N)
F_{chute}	=	force acting on payload calculated from load pins, “sensed” acceleration and known system mass (N)

II. Introduction

THREE upcoming interplanetary robotic exploration missions plan to use parachutes to decelerate their payload from supersonic speeds to the surface or lower atmosphere: Mars Sample Return (Mars, 2027), DaVinci (Venus, 2031), and Dragonfly (Titan, 2034). Each mission will likely require a customized parachute system to achieve its goals in its unique operating environment. When a parachute system is called upon to go beyond historical successes in terms of size or dynamic pressure, or when it is to be deployed in a different enough environment, a flight test in the upper Earth atmosphere is typically required for flight certification. For example, the Mars 2020 mission parachute design that

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helped land the Perseverance rover was tested during the third ASPIRE flight test in 2018 because it was to be deployed at a larger dynamic pressure than any previous Mars parachute. Although the different atmospheric compositions of the target planets/moons cannot easily be accounted for in the upper Earth atmosphere, the tests typically feature the full-size parachute and attempt to match the important non-dimensional parameters like Mach and Reynolds numbers, as well as expected dynamic pressure at deployment. Together, these parameters largely determine the forces the parachute will experience as well as its dynamics. Fluid-structure interaction (FSI) simulations could help accelerate the development and certification of new parachute systems once validated and best practices are well established. The Launch, Ascent, and Vehicle Aerodynamics (LAVA) software simulation suite was recently extended with the capability to perform FSI simulations by coupling its high-order Cartesian computational fluid dynamics (CFD) solver with adaptive mesh refinement (AMR) and immersed-boundary method (IBM) to a computational structural dynamics (CSD) solver based on the finite element method [1–3]. In our previous publications on this topic, we demonstrated through a grid convergence study that the predicted peak pull force is rather insensitive to further refinements to the CFD mesh (beyond 0.033 m or $1.55 \times 10^{-3} D_0$) and to further refinements to the CSD mesh (mean edge length commensurate with CFD mesh spacing) [1].

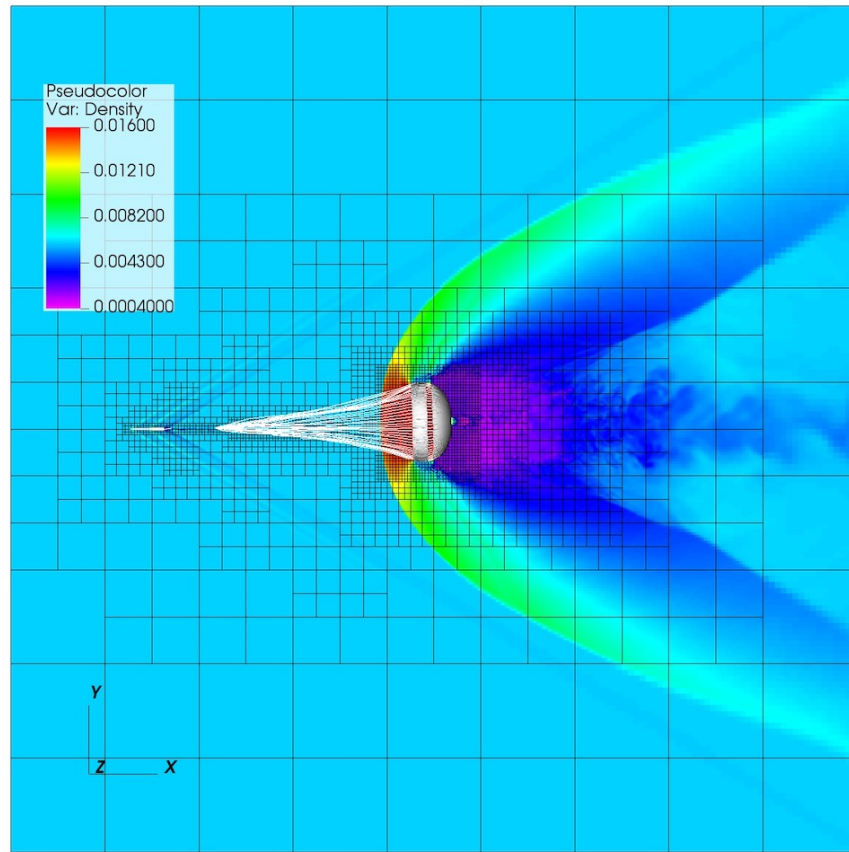
Following the publication of more detailed parachute system construction and material properties of the second and third ASPIRE flight test [4], we performed a set of simulations toward establishing best practices, where we learned the importance of including the small angle of attack recorded in the flight test to break the otherwise perfect symmetry of the problem, along with the importance of starting from an asymmetric canopy shape to avoid constructive interference of tension waves in the suspension lines during the inflation [3]. We also established that the non-dimensional pull force and parachute force (drag) at the inflation and rebound peaks are remarkably repeatable across the three ASPIRE flight tests (3% and 15% variation respectively) despite differences in parachute diameter and dynamic pressure when the parachute is released [3]. The flight-to-flight variation for the post-inflation trough is significantly larger at approximately 42% [3].

The focus of this work is to further the validation process and evaluate modeling choices with respect to the as-built parachute system. First, the effect of increasing effective porosity is investigated, knowing from recent experiments that the effective pore size of the parachute fabric can increase by a factor of two when under strains comparable to those expected once the parachute is fully open. Effective porosity is defined as $c_e = u/U$ where u is the airspeed through the fabric (directly related to the mass flow rate $\dot{m} = \rho v_n$) and U is determined from the differential pressure across the fabric $\Delta p = \frac{1}{2} \rho U^2$ [5]. We then rule out any effect from the mass of the confluence fitting by including it at the bridle to riser junction. The impact of including the viscous terms in CFD by solving the Navier-Stokes equations instead of the Euler equations is investigated. The effects are most salient on the canopy where a porous no-slip velocity boundary condition is applied, replacing the previously used porous slip condition. We noted a large sensitivity to how the radials are modeled in our previous work [3]. The radial gore seams are the main load paths from the canopy to the suspension lines (and vice versa) – see Fig 2. So in this work, we revisit modeling the gore seams as cables (Timoshenko beams) with a nearly 3x larger Young’s modulus than the suspension lines following the calculated Young’s modulus for the Mars 2020 parachute model in Rabinovitch et al. [4]. Having kept the payload traveling at a constant speed until now, we now include deceleration of the system to measure sensitivity of the predicted drag to this modeling choice. This is also to verify that we can capture the trends of the deceleration phase (after the rebound peak).

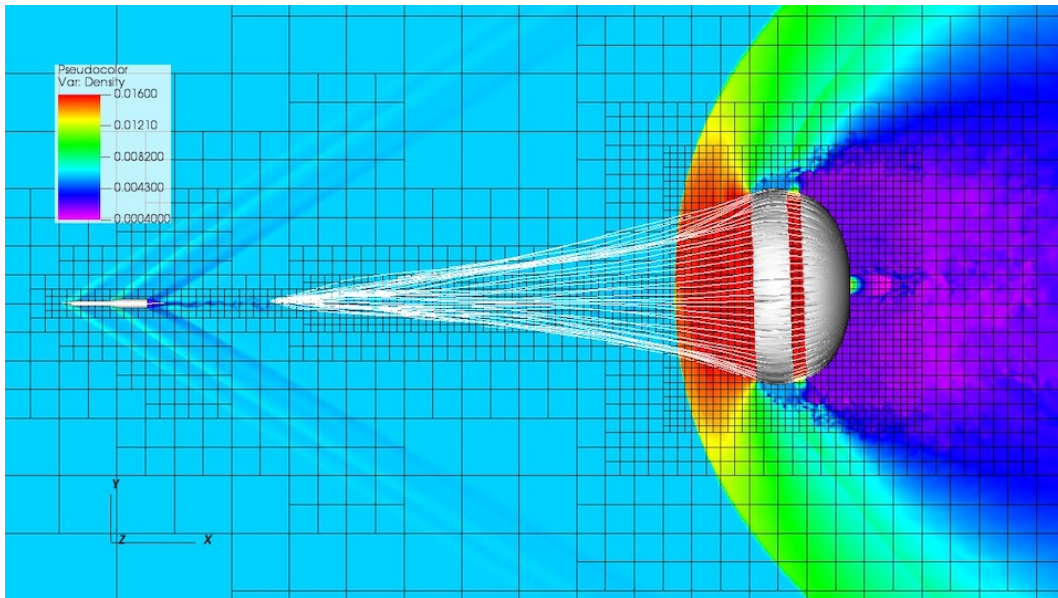
III. Methodology

The methodology established in Cadieux et al. [3] is leveraged for this work. The CFD parameters are summarized in Table 1 for completeness. We use the same CFD mesh as in Cadieux et al. [3] for all simulations without deceleration. The CFD mesh is automatically generated using static cylindrical and conical refinement regions covering the regions of interest – payload wake, parachute bow shock and wake – on the finest mesh: see Fig 1.

Table 2 summarizes the CSD parameters. The previously developed CSD mesh [3] is used for all simulations. Coupling is achieved via the transfer of tractions (pressure forces only at this time) at shared nodes between the inflated (water-tight with finite thickness) triangulation of the parachute and its infinitely thin finite element mesh counter-part at the end of a full CFD step. The CSD solver then catches up by performing as many smaller explicit steps as necessary (typically 10 to 20) to avoid violating the time integration scheme’s stability restrictions, and then transfers the displacements of each shared node to the CFD parachute surface. This loose-coupling approach is formally first-order accurate in time, but we established in previous work that the dominant error is linked to the CFD’s spatial discretization [1] because the explicit time steps are very small with $\Delta t \lesssim O(10^{-5})$ s. Note that we do not consider the forces of the fluid on the cable elements, nor do the displacements of the cable elements influence the fluid in any way.



(a) Full domain



(b) Zoomed-in view

Fig. 1 Slice of the CFD mesh showing boxes of 16^3 cells in black, density on the cut plane, with the payload and parachute system geometry shown in white at the post-inflation trough where the bow shock moves furthest upstream from the parachute for the baseline case.

Table 1 CFD parameters for FSI simulations.

Parameter	Value	Note/Units
Discretization type	Conservative finite difference	In Shu-Osher form
Convective flux scheme	Weighted Essentially Non-Oscillatory (WENO)	5 th order Z-weights [6]
Flux function	Rusanov Flux-Vector-Splitting	With characteristic transformation
Viscous flux scheme	Conservative 2 nd order centered	
Time integrator	Strong Stability-Preserving Runge-Kutta (SSPRK)	Explicit 3 rd order
CFL number	0.9	
Minimum mesh size	0.0333	m
Maximum mesh size	1.0666	m
Refinement levels	6	2:1 ratio between levels
Mesh type	Static & Adaptive	Cylindrical refinements
Mesh size	$\approx 300 \times 10^6$	Cells
Domain size	$L_x = L_y = L_z = 153.6$	m

Table 2 CSD parameters for FSI simulations.

Parameter	Value	Note/Units
Discretization type	Finite element method	
Fabric elements	MITC3 triangular shells	with additional drilling DOF
Cable elements	Timoshenko beams with 6 DOF	No resistance to compression
Mass matrix	Lumped	
Time integrator	Explicit 2 nd order centered	with high-freq damping
Critical time step ratio	0.3 - 0.4	
Minimum edge length	0.0254	m
Maximum edge length	0.051	m
Mesh size	$\approx 300 \times 10^3$	Elements

This simplifying assumption is currently justified on the basis that the cross-sectional area of these cables with respect to the flow is minuscule compared to the size of the parachute, even in its initial folded accordion shape. However, we acknowledge that the bridles and riser's effect on the flow may become important indirectly by increasing mixing or blockage in the payload wake. This may be the subject of future investigations, but is outside the scope of this work. The full structural mesh is reproduced here in Fig 2 for reference, with each of its components clearly labeled. Consistent with previous work, the payload is considered rigid and static for all simulations except the decelerating cases, where it is rigid but has full six degrees of freedom of motion (6 DOF free flight) based on the aerodynamic forces acting on it as well as the tension of the bridles at their attachment points on the payload. Porosity of the canopy fabric is modeled by allowing a certain amount of mass flow through the canopy based on the pressure difference across its thickness by analytically solving the Darcy-Forcheimer equations [1] in terms of the mass flux, $\dot{m}$,

$$\dot{m} = \rho_1 v_{n,1} = \rho_2 v_{n,2} = -\frac{1}{2K_p\beta} \left(\mu(\bar{T}) - \sqrt{\frac{2K_p^2\beta(p_2^2 - p_1^2)}{W\bar{T}}} + \mu(\bar{T})^2 \right), \quad (1)$$

where $\bar{T} = \frac{1}{2}(T_1 + T_2)$ is the temperature at the center of the porous fabric taken to be the average of the two sides, p_2 corresponds to the pressure on the high-pressure side of the fabric, p_1 the pressure on the low-pressure side of the fabric, ρ is density, v_n is the velocity normal to the surface, μ is the temperature-dependent dynamic viscosity following Sutherland's law, K_p is the permeability coefficient, β is the inertial permeability coefficient, R is the gas constant, and W is the width of the porous medium. An effective porosity of $c_e = 4.5\%$ is imposed in our baseline simulations based on the PIA-C-7020D fabric specification and experiments done by Cruz et al. [5]. K_p and β were determined from

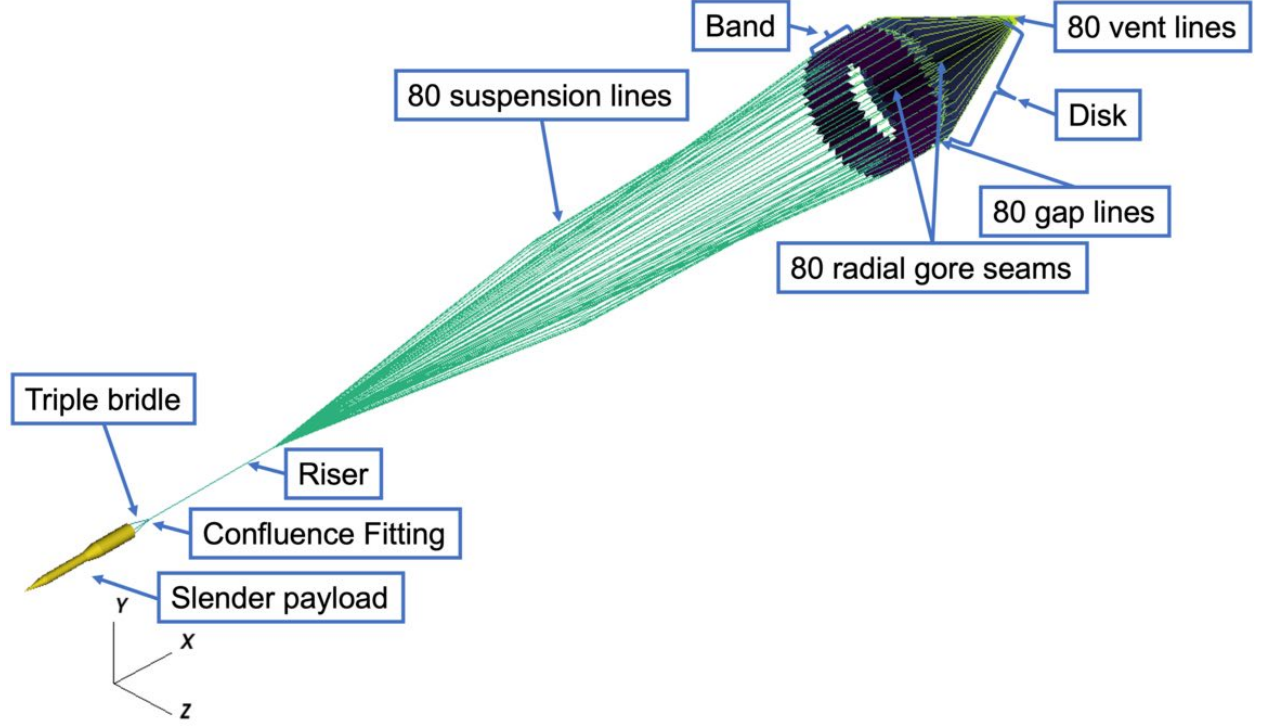


Fig. 2 Complete structural dynamics mesh of ASPIRE system colored by component, showing the bridles, confluence fitting, riser, suspension lines, gap and vent lines, as well as the radial gore seams.

Table 3 Material properties used in the finite element model are largely consistent with Rabinovitch et al. [4].

Component name	Element type	Young's modulus (Pa)	Poisson ratio	Density (kg/m ³)	Width (m)	Thickness (m)
Bridles	Timoshenko beam	1.6680×10^{10}	0.4	705.13	0.03908	-
Riser	Timoshenko beam	1.8600×10^{10}	0.4	751.99	0.04778	-
Suspension lines	Timoshenko beam	1.8202×10^{10}	0.4	484.66	0.00506	-
Gore seams	Timoshenko beam	3.8705×10^{10}	0.4	484.66	0.00506	-
Broadcloth	MITC3	9.4484×10^8	0.4	533.95	-	7.6200×10^{-5}
Band leading edge	MITC3	1.3568×10^{10}	0.4	454.07	0.0254	1.0583×10^{-3}
Band trailing edge	MITC3	1.3568×10^{10}	0.4	454.07	0.0254	1.0583×10^{-3}
Disk leading edge	MITC3	1.3568×10^{10}	0.4	454.07	0.0254	1.0583×10^{-3}
Disk trailing edge	MITC3	1.0979×10^{10}	0.4	525.54	0.0254	2.5400×10^{-3}

fitting experimental data using eq. 1 [7]. Increasing the effective porosity by a factor of 2 to $c_e = 9\%$ is done by halving β from 1.9×10^6 to 0.95×10^6 and increasing K_p by a factor of 4 from 8.5×10^{-12} to 3.4×10^{-11} . Porosity and other wall boundary conditions are imposed through an immersed boundary method which is an extension of the ghost-cell method [8] to handle arbitrary thin geometry compared to cell size via ghost-in-fluid cells [9]. Contact mechanics are modeled by identifying which structural shell elements may come into contact and imposing an equal and opposite momentum (coefficient of restitution of 1) to ensure the elements do not intersect. The material properties used in the structural dynamics mesh are summarized in Table 3.

The startup procedure for the FSI simulations was simplified (compared to [3]) to reduce the number of steps while retaining the same criteria:

Table 4 Flow conditions selected for FSI simulations corresponding to line stretch in ASPIRE SR03 flight test [10].

Quantity	Value	Unit
M_∞	1.88	-
q_∞	1028.44	Pa
ρ_∞	6.0171×10^{-3}	kg/m ³
α	0.9	°
Reynolds number per meter	2.2784×10^5	1/m
Prandtl number (Pr)	0.7	-
Ratio of specific heats γ	1.4	-
Gas constant (R)	287.0025	J/(kg K)
Sutherland's Law μ_{ref}	1.7084×10^{-5}	N s / m ²
Sutherland's Law T_{ref}	273.15	K
Sutherland's Law T_S	110.0	K

- 1) Pre-inflate the parachute using structural dynamics solver coupled with contact mechanics module using a prescribed force normal to the canopy surface augmented with a radial force with asymmetric analytical circumferential variation for 0.016 s.
- 2) Perform a CFD simulation of the rigid and static ASPIRE payload with the static pre-inflated parachute held rigid until aerodynamic loads of both payload and parachute have reached a statistically stationary state and the payload wake has reached beyond the location of the parachute (55 m downstream), roughly 0.25 seconds.
- 3) Perform coupled FSI simulation restarting from the static and rigid CFD simulation.

Initial flow conditions for all simulations are described in Table 4 and correspond to conditions measured or inferred from available atmospheric data at the line stretch event during the ASPIRE SR03 flight test, which occurred 1.027 s after parachute deployment. Each simulation was performed using 50 skylake compute nodes with 40 cores each over a 48 hour period for a total of 96000 core-hours, which in most cases resulted in roughly 0.6 s of “real time” simulated for the FSI portion and 0.25 seconds of CFD-only time simulated to establish the flow.

IV. Results: Overview

To reduce the number of simulations, we make each parameter choice binary, and start from our previous best practices denoted as “baseline” in Table 5 that is explained in detail in Section IV.A. We also only continue to use a given parameter value if it improves the agreement of predicted drag to the drag calculated from measurements made during the ASPIRE SR03 flight test, or if it is objectively more representative of the as-built parachute and does not significantly degrade the accuracy of the model’s predictions. This brings the total number of simulations performed and analyzed for this work to six, with the differences between each outlined in Table 5. Note that the flight tests compute pull force F_{pull} as the sum of the axial component of the tension measured at the load pins. And the parachute drag force F_{chute} is then approximated using F_{pull} and the acceleration recorded onboard the payload a_p as $F_{chute} = F_{pull} - m_c a_p$, where m_c is the mass of the parachute system. The flight test derived F_{chute} thus contains the parachute aerodynamic drag F_{aero} and some tension wave loads super-imposed on top. In FSI simulations, the parachute aerodynamic drag F_{aero} is directly available as the typical integral of pressure and viscous (where applicable) forces over the surface triangulation representing the parachute. A direct FSI analog to flight test F_{chute} is especially difficult to compute for cases where acceleration is not modeled so we focus on comparing F_{aero} from FSI to F_{chute} from the flight test in their non-dimensional form as coefficients of axial force C_A . The FSI simulations start with a fairly large cross-sectional area compared to the flight test parachute at line-stretch. As a result, the FSI simulations inflate much faster. So we apply a time shift $\Delta t_{shift} \approx t_{peak,flight} - t_{peak,FSI}$ to the FSI results in order to align the FSI peak inflation time $t_{peak,FSI}$ to that of the flight test $t_{peak,flight}$. These time shifts vary from simulation to simulation and are also shown in Table 5.

Table 5 Simulation names and associated parameter differences.

Section	Simulation Name	Effective Porosity	Confluence Fitting	Viscous Effects	Gore Seams	Free Flight Deceleration	Δt_{shift}
IV.A	Baseline	4.5%	No	No	No	No	0.317
IV.B	Double Porosity	9.0%	No	No	No	No	0.290
IV.C	Confluence	4.5%	Yes	No	No	No	0.317
IV.D	Confluence Viscous	4.5%	Yes	Yes	No	No	0.305
IV.E	Confluence Viscous Stiff Radials	4.5%	Yes	Yes	Yes	No	0.315
IV.F	Confluence Viscous Free Flight	4.5%	Yes	Yes	No	Yes	0.322
IV.G	Confluence Viscous Stiff Radials Free Flight	4.5%	Yes	Yes	Yes	Yes	0.314

A. Baseline

FSI simulations are setup to follow our previous best practices: angle of attack $\alpha = 0.9^\circ$, asymmetric initial canopy shape, no radial stiffeners as established in Cadieux et al. [3]. Fig 3 illustrates various important points during the parachute inflation process for this previous simulation that serves as a baseline for this work. Fig 3a shows an overview of the flow topology at the start of the FSI part of the simulation. Notice the attached weak bow shock emanating from the payload's nose, the expansion fan emanating from the transition from the nose cone to the first cylindrical section, the attached weak shock due to the transition from the first cylindrical section to the second conical region, an expansion fan at the transition from the second conical region to the second cylindrical region, and a stronger expansion fan at the aft end of the payload. Just downstream of the aft end of the payload, the flow is separated and quickly transitions to turbulence. The wake of the payload extends all the way to the mouth of the parachute where it interacts with the bow shock associated with the effective cross-sectional area of the asymmetrically folded and porous parachute.

The outline of the bow shock is just barely visible upstream of the mouth of the parachute and curves downstream just outside the band. The gap between the band and the disk sees subsonic flow due to the stronger bow shock associated with the disk due to its larger effective cross-sectional area. This region of subsonic flow extends radially outside the disk and effectively joins with the shock from the band. Over the disk's outer surface, the flow then accelerates as it experiences a decrease in blockage and then separates, leading to large shear layers and some regions of subsonic flow in its wake. The flow inside the disk is accelerated through the vent hole, which acts like a convergent nozzle and causes a small barrel shock to form just downstream of the vent hold. As expected from high-speed video taken from the flight test, the disk flattens first and pulls the band toward itself as seen in Fig 3b during the initial moments of the inflation.

Consistent with our previous FSI simulations, it shows that much of the bow shock is ingested by the parachute, delineated by the sharp curved yellow-to-blue transition line that appears in the gap between band and disk. As the disk inflates further, it pulls the band out with it and pushes the bow shock further upstream, as is seen at the moment of peak drag during the inflation in Fig 3c. The increasingly pressurized disk turns the vent hole into a higher pressure ratio supersonic nozzle, resulting in a strong barrel shock that reaches a maximum Mach number of nearly 4. The same pressurization also pushes the flow out of the gap between disk and band at supersonic speeds as is seen by the pocket of yellow transitioning radially to red in that region on the image. The flow on the outer skin of the band is also starting to separate, whereas it was mostly attached up until this point – see Fig 3a and 3b.

The over-pressurization of the disk pushes the bow shock further upstream, and allows the elastic restoring forces in the disk and band to momentarily win, reducing the overall cross-sectional area of the parachute as seen in Fig 3d, and by extension reaching a trough in the drag developed by the parachute. The flow is fully separated over the outer portion of the band at this point, as seen by the angled blue triangle-like shapes on the Mach slice. The bow shock having moved upstream and reduced in strength, the wake interactions with it are more visible, and the barrel shock size and strength downstream of the vent hole is reduced compared to Fig 3c. As the forces of flow pressurization and elastic

restoring force in the parachute come close to equalizing, the bow shock moves closer to the edge of the band, and the overall parachute cross-sectional area and drag reach their post-inflation rebound peak in Fig 3e. After this point in time, the parachute drag continues to oscillate in time but with decaying amplitude.

B. Effect Of Porosity

Increasing the effective porosity from 4.5% to 9% has significant repercussions on the pressure distribution on the parachute at the start of the FSI simulation. The bow shock that develops due to the cross-sectional area of the canopy typically moves just upstream of the band in our previous simulations as we integrate forward in time with the parachute rigid. The increased porosity of the parachute fabric has a similar impact to reducing the cross-sectional area that the oncoming flow encounters, so the bow shock instead stabilizes between the disk and band part of the canopy, named the gap (in disk-gap-band parachute designs) – see Fig 4. It also reduces the velocity differential in the wake of the parachute, with fewer pockets of deep blue as more flow is allowed to pass through the canopy. Somewhat counter-intuitively, this lowers the amount of shear experienced by the supersonic flow through the choked vent hole, and thus leads to the appearance of more classical Mach diamonds.

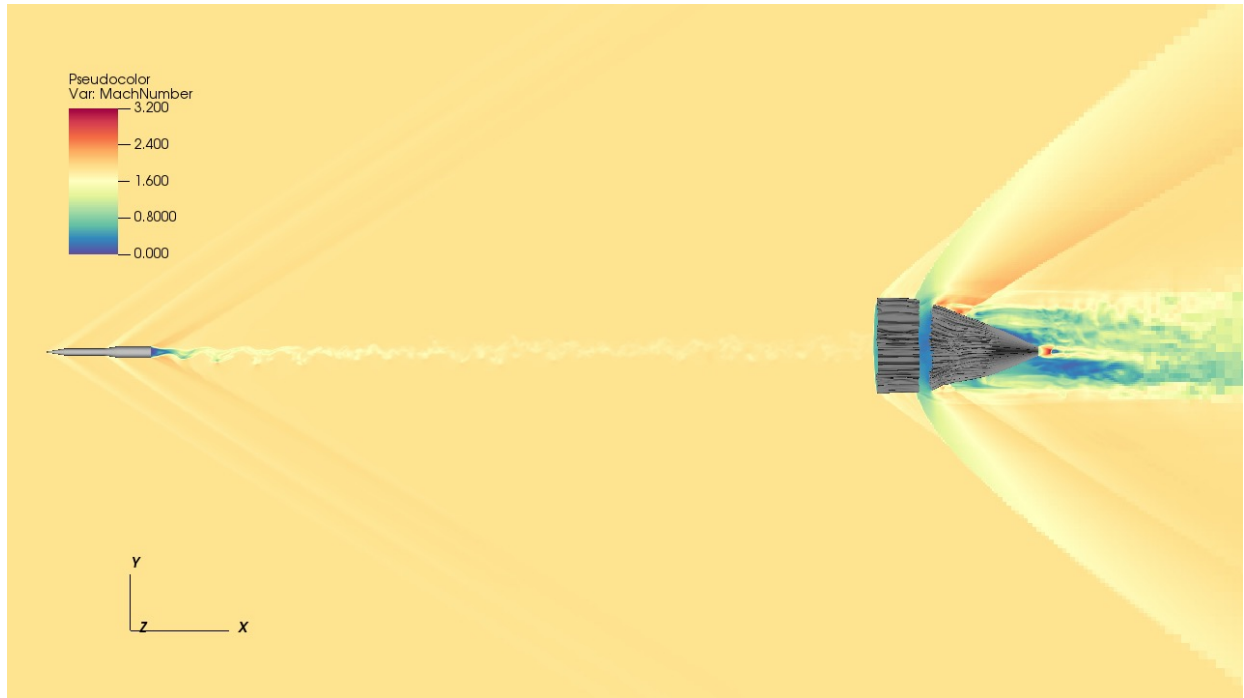
The location of the bow shock at the gap between band and disk at the start of the FSI simulation has a severe effect on the drag during inflation where the bow shock is ingested earlier, causes larger pressures on the disk earlier in time, and causes stronger oscillations as any slack in the lines caused by the asymmetric pre-inflation is made taut. After this initial correction, the behavior is very close to our previous best practice results as shown in Fig 5 and when comparing Figs 3 and 4 from (b) to (e). The added porosity has relatively little impact on the peak parachute force despite how different the initial conditions are at the start of this coupled FSI simulation (compared to previous simulations) and we recover very similar behavior beyond the inflation peak. The drag developed by the parachute during inflation does differ significantly in that the inflation time is increased by 0.05 s, and has a slightly more pronounced inflection point as seen in Fig 5 around $t = 0.33$ s after line-stretch.

C. Effect Of Confluence Fitting

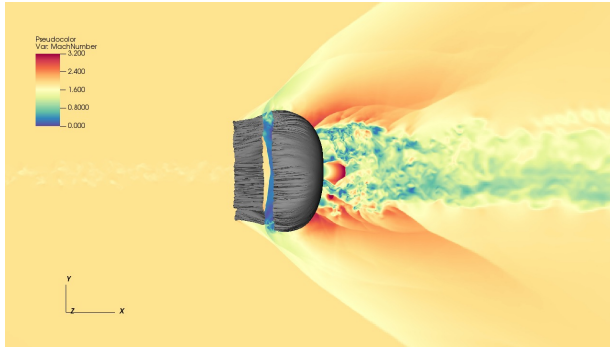
The hardware that ties the three bridles to the riser is called the “confluence fitting”. Its mass was measured to be 3.7 kg. The entire parachute system has a mass of roughly 80 kg. So at 5% of the total parachute mass and located much closer to the payload, it seemed appropriate to model the impact of its mass on the tension recorded at the load pins. As a first step, we ignore the physical volume of the confluence fitting in the CFD, which is consistent to our best practices so far where we have ignored the volume of all cabling. The fitting is then modeled as an increased effective density on the first finite element of the riser (length of 0.05 m) to ensure that this element’s mass is consistent with the added mass of the confluence fitting. There were no visible changes in the overall dynamics, but they are included for completeness in Fig 6. As expected, this change had zero impact on the drag developed by the parachute as shown in Fig 7 where the two simulation curves are on top of each other.

D. Effect Of Viscosity

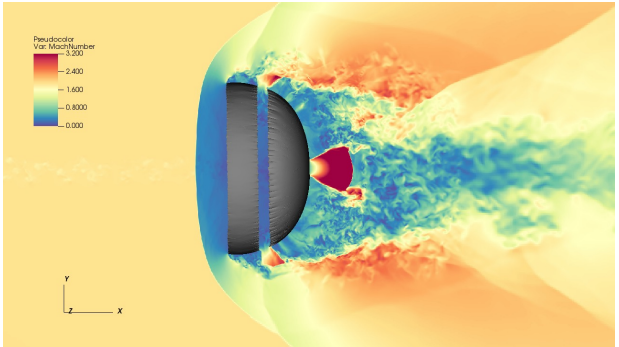
So far, our best practice has been to model the flow using the Euler equations, thus neglecting viscous effects. We feel this is justified because the loads on the fully open parachute are dominated by pressure drag by two orders of magnitude or more compared to viscous drag. However, during the inflation process, we observe that the flow is not fully separated behind the canopy when using the Euler equations and imposing slip on the tangential velocity at the canopy. This results in areas of strong flow acceleration where the flow turns and is “forced” to follow the shape of the parachute. In reality where viscous forces apply, this is highly unlikely to occur, as the flow would separate much sooner. To test whether viscous effects will change the behavior of the parachute force, we solve the full compressible Navier-Stokes equations along with a no-slip porous immersed boundary condition for the parachute. To reduce the scope of the change, we leave the payload with its original slip boundary condition (because capturing the thin attached boundary layers there would likely require significantly smaller CFD mesh size, increasing our turnaround time). As a first step, we do not change our CFD mesh, and thus make no attempt to meet a $\Delta y^+ \leq 1$ requirement for wall-resolved large-eddy simulation (LES). As such, we fully expect that any attached boundary layer size on the canopy will be over-predicted and the wall shear stress under-predicted. The purpose is to see if the detached shear layers and enhanced mixing in the wake during the inflation process have a significant impact on the dynamics of the parachute force. The impact of this change remains subtle during the inflation. The initial condition changes to where the bow shock moves further upstream of the band due to the added blockage caused by the boundary layers, and the flow behind the folded



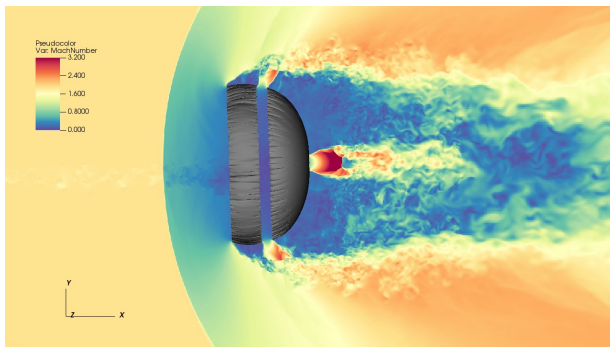
(a) Initial condition for FSI



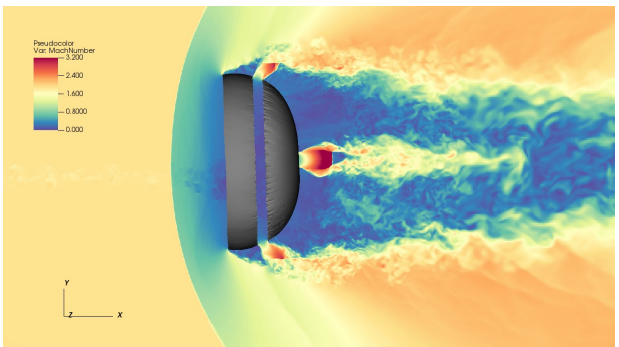
(b) Inflection point in drag curve



(c) Inflation peak

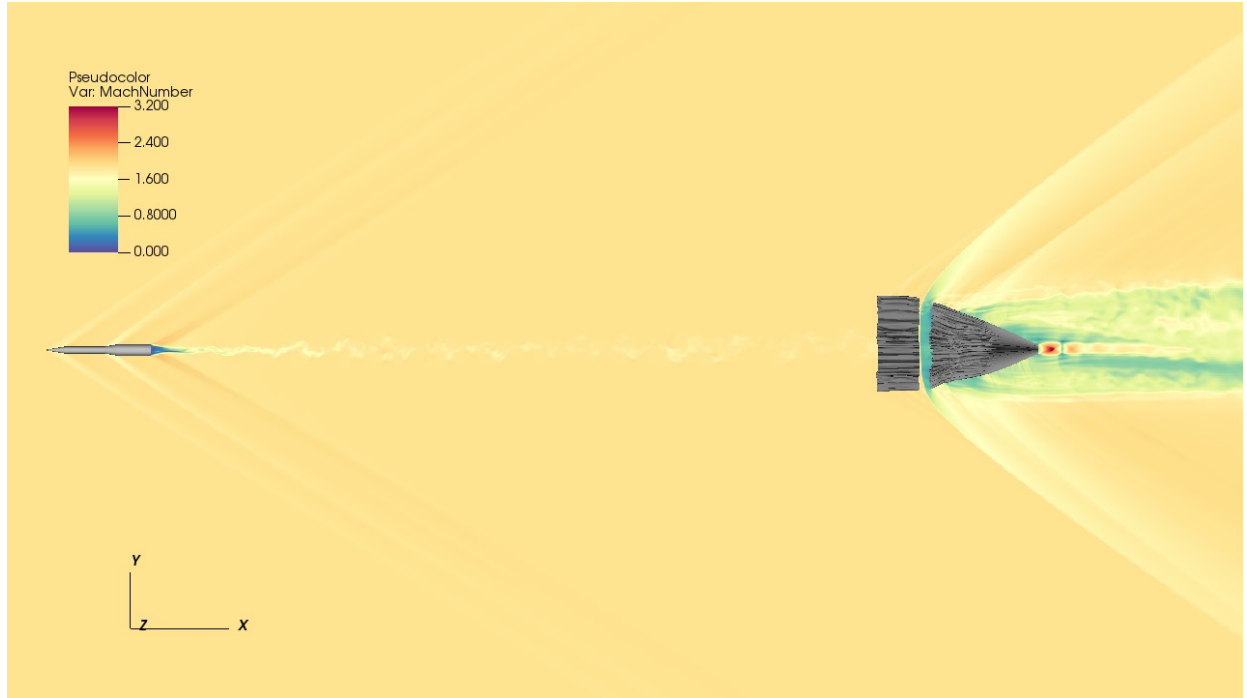


(d) Post-inflation trough

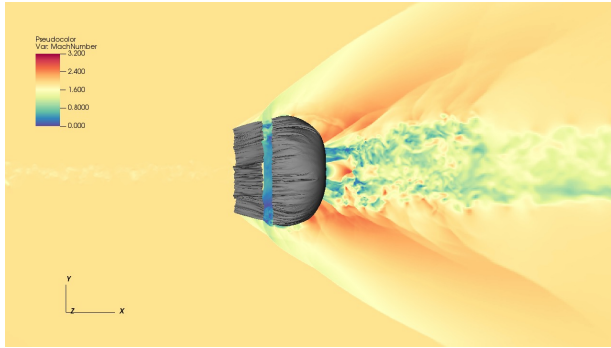


(e) Post-inflation rebound peak

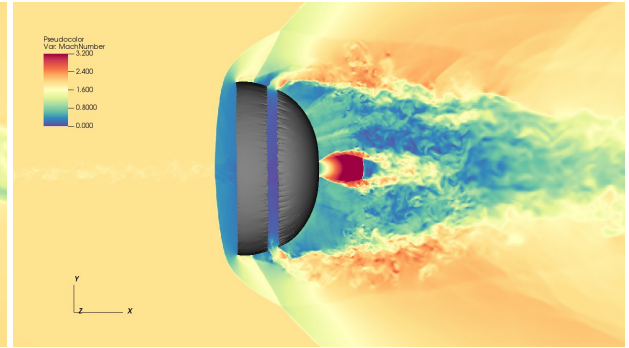
Fig. 3 Snapshots from a simulation with previous best practices: angle of attack $\alpha = 0.9^\circ$, asymmetric initial canopy shape, no radial stiffeners [3]. Each shows Mach number on a cut plane, as well as the geometry of the payload and parachute in gray.



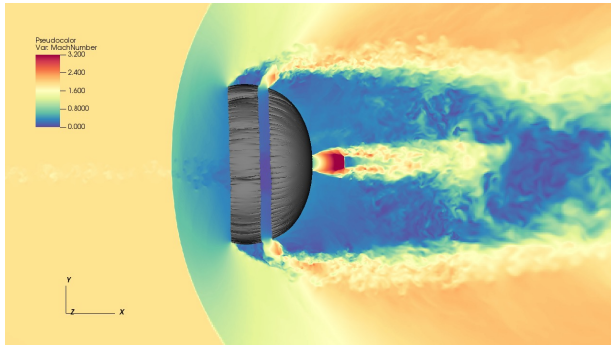
(a) Initial condition for FSI



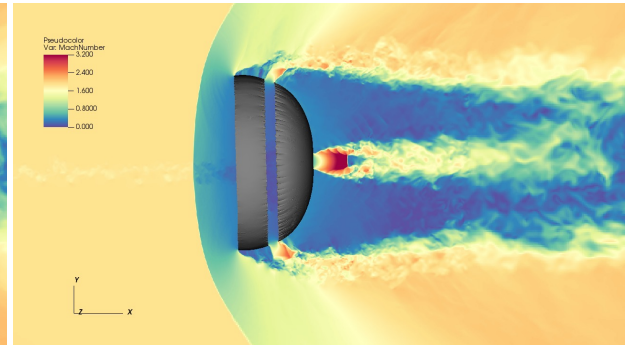
(b) Inflection point in drag curve



(c) Inflation peak



(d) Post-inflation trough



(e) Post-inflation rebound peak

Fig. 4 Snapshots from a simulation with twice the effective porosity compared to our baseline. Each shows Mach number on a cut plane, as well as the geometry of the payload and parachute in gray.

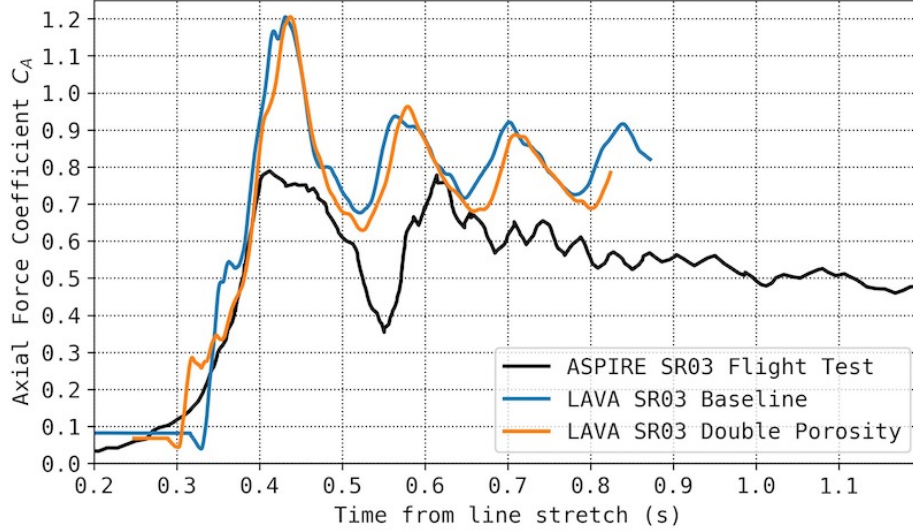
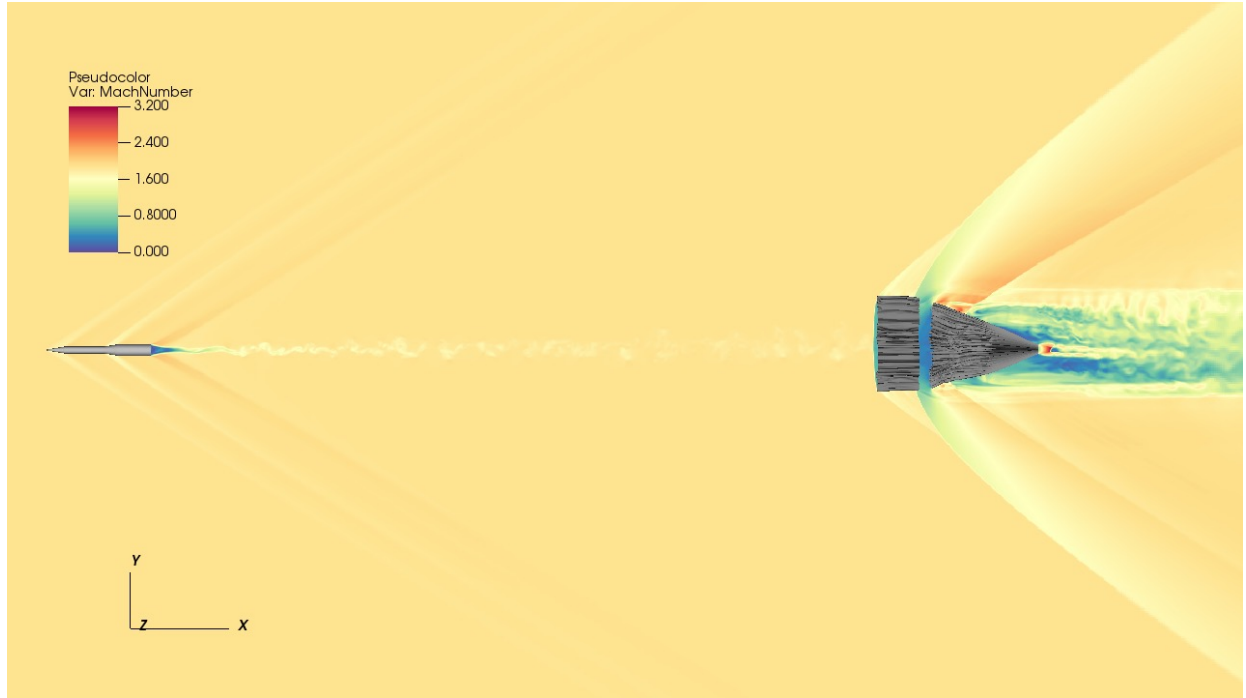


Fig. 5 Axial force coefficient C_A comparison between ASPIRE measurements and LAVA FSI simulations with effective porosity of 4.5% (baseline) and 9% (double porosity).

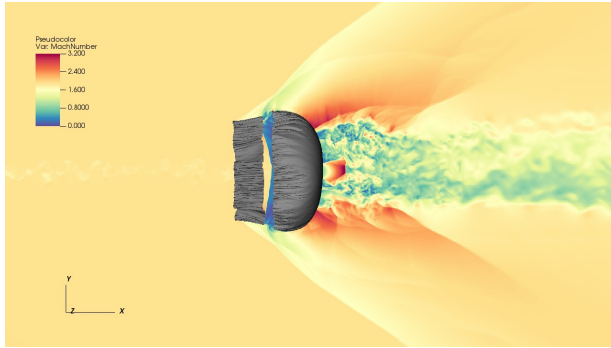
and pre-inflated canopy is fully separated and more turbulent – see Fig 8a. The flow also separates over portions of the band, indicated by areas of dark blue stemming from the leading edge of the band in the bottom half, and from the middle of the band on the upper half, whereas flow over the band was fully attached in all prior simulations. This also affects the inflation process before the peak where the flow downstream of the disk remains largely separated in Fig 8b whereas it was attached and accelerating over the “shoulder” of the disk in Fig 6b. Viscous effects damp the initial oscillations in drag and slightly reduce the peak drag as shown in Fig 9. It also slightly damps the amplitude of the oscillations so far associated with the “breathing” motion after the rebound peak in the axial force coefficient. Note that these damping effects could be exaggerated because our mesh resolution is not sufficient to capture the full physics of any attached shear layers on the canopy before they separate, but should be adequate in regions and times where the flow separates very close to the leading edge of the band and the disk.

E. Effect Of Modeling Gore Seams

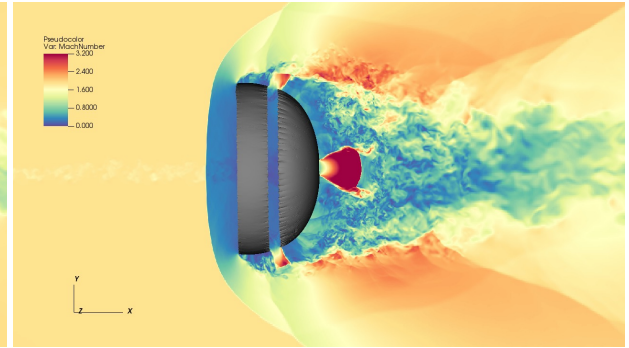
The seams binding the 80 panels of the disk and band together are one of the main load paths to transmit the forces to the suspension lines. They measure roughly one inch in width and feature four layers of fabric folded into hooks and sown together with high-tensile strength wire. This is challenging to model at the macro level for a number of reasons, chief among which is that the Young’s modulus and shear modulus of the gore seams has not been characterized experimentally to the authors’ knowledge. In previous work, we used the same simplifying assumption as the Jet Propulsion Laboratory (JPL) and Stanford team and modeled these gore seams as continuous suspension lines (40 instead of 80) [11], with the same material properties as suspension lines in Table 3. This gets the basic load path configuration correctly, but inherently assumes that the gore seams have the same inertial and material properties as the suspension lines, which we know is incorrect. We noted that using this simplifying assumption created the opportunity for constructive interference of tension waves, especially when starting from a symmetrically folded canopy shape [3]. However, ignoring the presence of the gore seams is not in line with what is known about the load path on the parachute. To address this, we return to our previous approach of modeling the gore seams as cable elements (Timoshenko beams), but this time with a different Young’s modulus (3.8704763×10^{10} Pa) based on the calculated values for the gore seams used in the LS-Dyna model by JPL and Airborne systems reproduced in Table 3 labeled as “radial gore seams”. The factor of nearly three increase in stiffness should change the character of the constructive interference of tension waves observed previously by causing any waves to be partially transmitted and partially reflected at the suspension line to gore seam interface. We continue to use the same radius as the suspension lines to prevent the explicit time step restriction associated with matching the measured thickness of the gore seams which is roughly 3×10^{-4} m, which is more than eight times smaller than the radius of the suspension lines. This might over-estimate the mass of the gore seams, but should provide a more representative stiffness and load path.



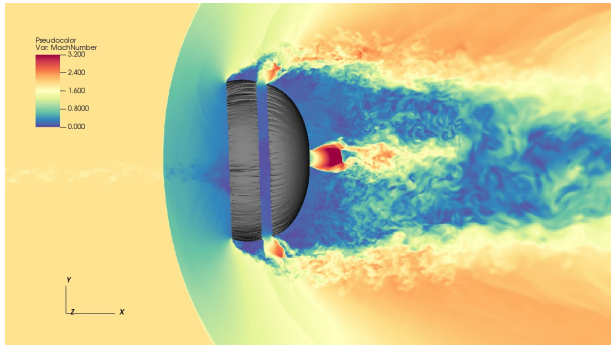
(a) Initial condition for FSI



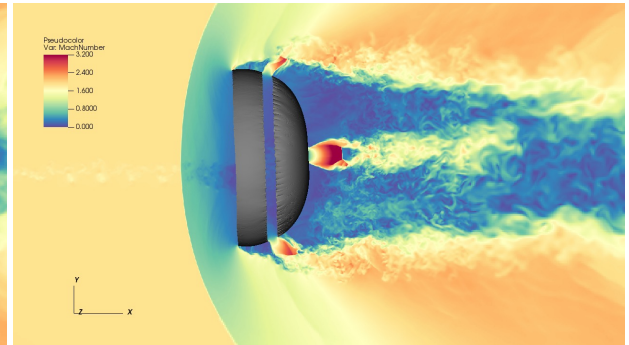
(b) Inflection point in drag curve



(c) Inflation peak



(d) Post-inflation trough



(e) Post-inflation rebound peak

Fig. 6 Snapshots from a simulation with the added mass of the confluence fitting at the bridle to riser junction. Each shows Mach number on a cut plane, as well as the geometry of the payload and parachute in gray.

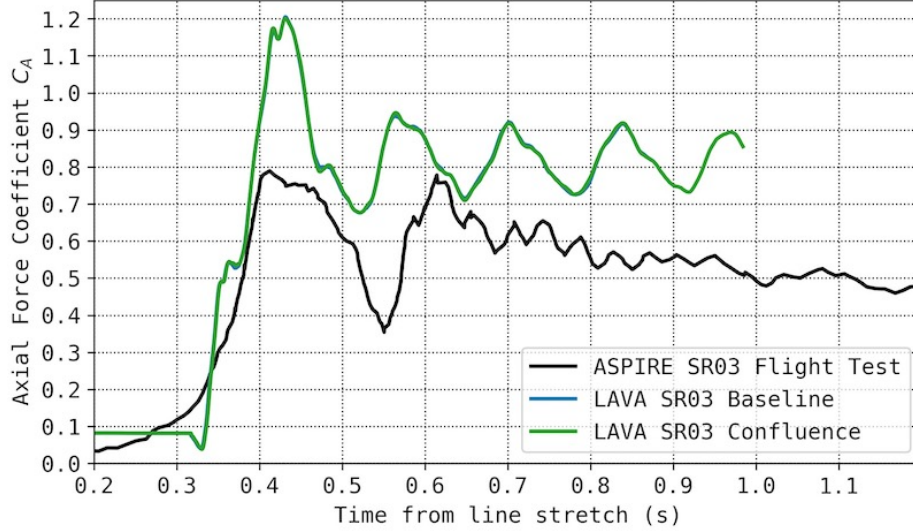


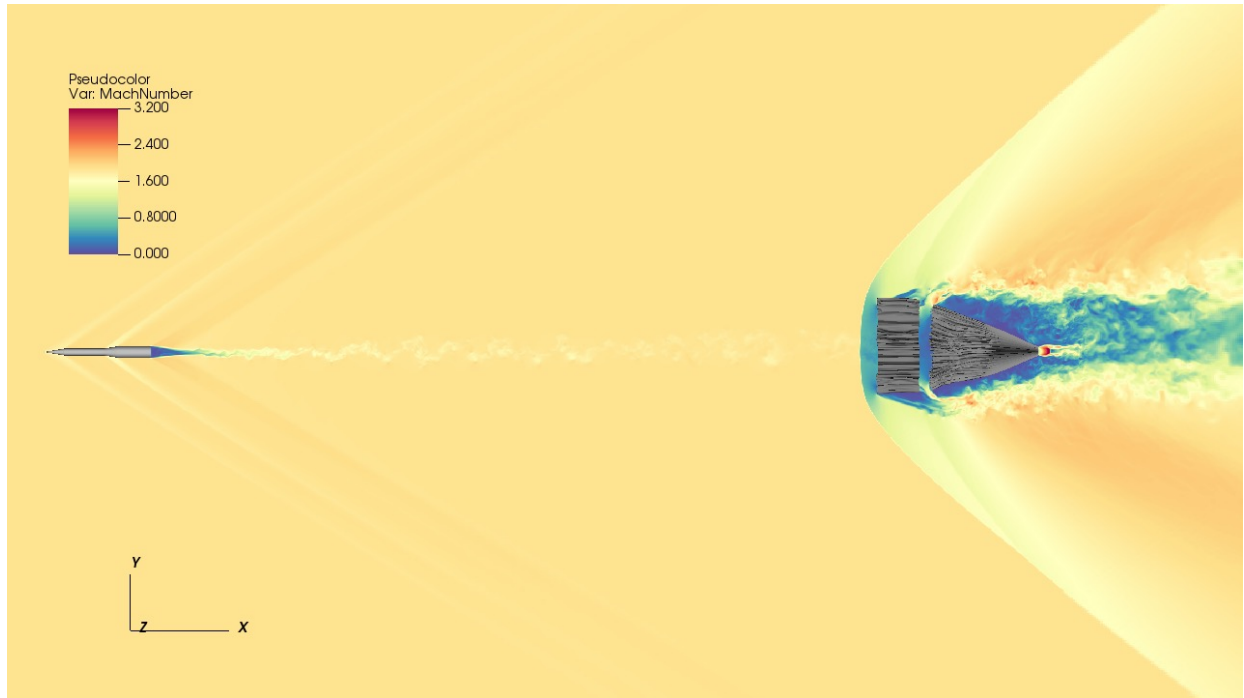
Fig. 7 Axial force coefficient C_A comparison between ASPIRE measurements and LAVA FSI simulations with and without added mass of confluence fitting.

The overall character of the inflation is almost identical, but the shape of the parachute is different when comparing Fig 10 to Fig 8. The higher stiffness at the gore seams results in local curvature developing in between seams, with the seams preventing further displacement or stretching of the fabric. This behavior is observed in photos of the flight test, and is in fact what the manufacturer is aiming for: by reducing the effective local curvature, the fabric stress is reduced and localized to the intended load path through the lines and the seams – fabric stress is routinely estimated through its proportionality to local radius of curvature as $\sigma \approx q C_p R_p / 2$, where q is the dynamic pressure, $C_p = (p - p_\infty) / q_\infty$ is the local coefficient of pressure, and R_p is the local radius of curvature of the fabric. This is in fact the stated purpose of vent line foreshortening, where the vent lines are made to be shorter than the diameter of the vent hole. Vent line foreshortening prevents the trailing edge of the disk from ever being stretched flat, and instead forces the fabric to have a relatively small local radius of curvature between each gore seam where the vent lines are attached. Although vent line foreshortening is not modeled in our simulations yet, the effect of increasing the gore seams’ stiffness is very similar at the disk’s leading edge, and throughout the band as seen in Fig 10b to 10e.

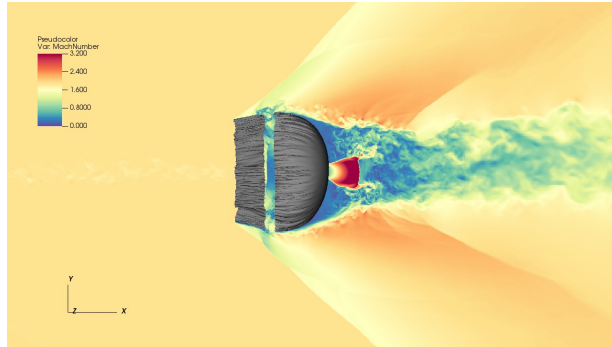
The change in the aerodynamic drag produced by the parachute with radial gore seams is drastic. This can be seen in Fig 11 which shows a nearly 20% drop when comparing the simulation with stiff radials compared to the previous one without. The addition of the stiff radials reduces the maximum drag produced by the parachute F_{aero} by restricting its maximum cross-sectional area. This is deduced by the well-known Mach-area relationship, but is also easily confirmed by looking at the size of the cross-section near the peak-drag time in Fig 12. In this figure, the canopy of both parachutes are plotted, with the baseline (no stiff radials) in dark purple in the background, and the stiff radials canopy in yellow on top. Note how the yellow canopy belonging to the parachute with stiff radials lies largely inside the larger area of the dark purple canopy at the instant of peak drag. The difference is small for the band’s outermost radius, but it is rather stark for the disk, where the purple canopy is noticeably larger. The difference in disk diameter is nearly 4% of the as-constructed disk diameter, translating to an approximately 16% reduction in area and accounting for the majority of the difference in drag in Fig 11. The change in standoff distance and strength (pressure ratio from upstream to downstream of the shock) of the bow shock likely accounts for the rest of the drag drop.

F. Effect Of Deceleration

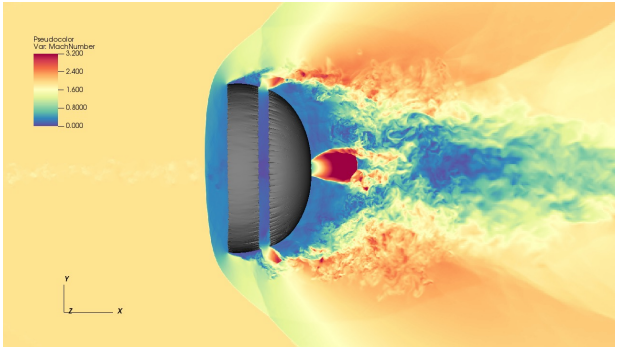
We perform FSI simulations where the payload can move and decelerate as it experiences the large drag provided by the parachute. This capability was recently implemented in a general way to support relative motion of various objects with respect to the Cartesian background mesh. It does bring additional cost, as the payload moves at each Runge-Kutta sub-step and information necessary to apply immersed boundary conditions are recalculated to maintain the design order of accuracy of the time integration scheme for the payload’s motion – this is important for second derivative quantities like acceleration. This contrasts with our loose-coupling with the CSD solver which only deforms the parachute once



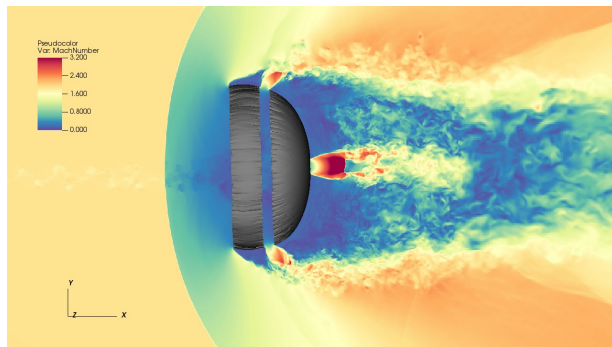
(a) Initial condition for FSI



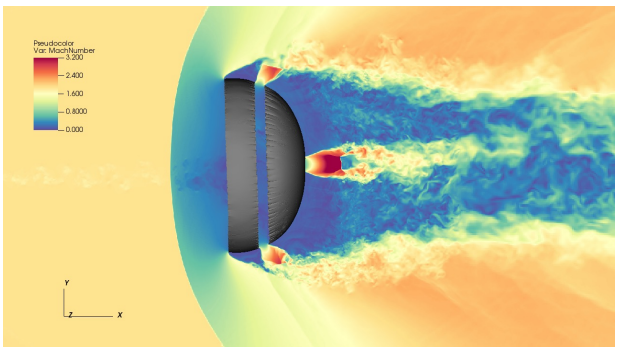
(b) Inflection point in drag curve



(c) Inflation peak



(d) Post-inflation trough



(e) Post-inflation rebound peak

Fig. 8 Snapshots from a simulation with the added mass of the confluence fitting at the bridle to riser junction and viscous effects. Each shows Mach number on a cut plane, as well as the geometry of the payload and parachute in gray.

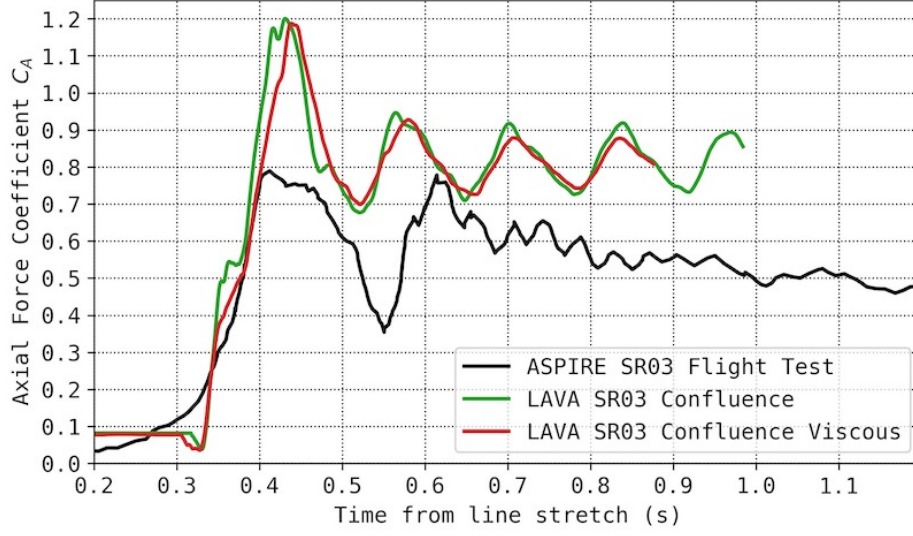


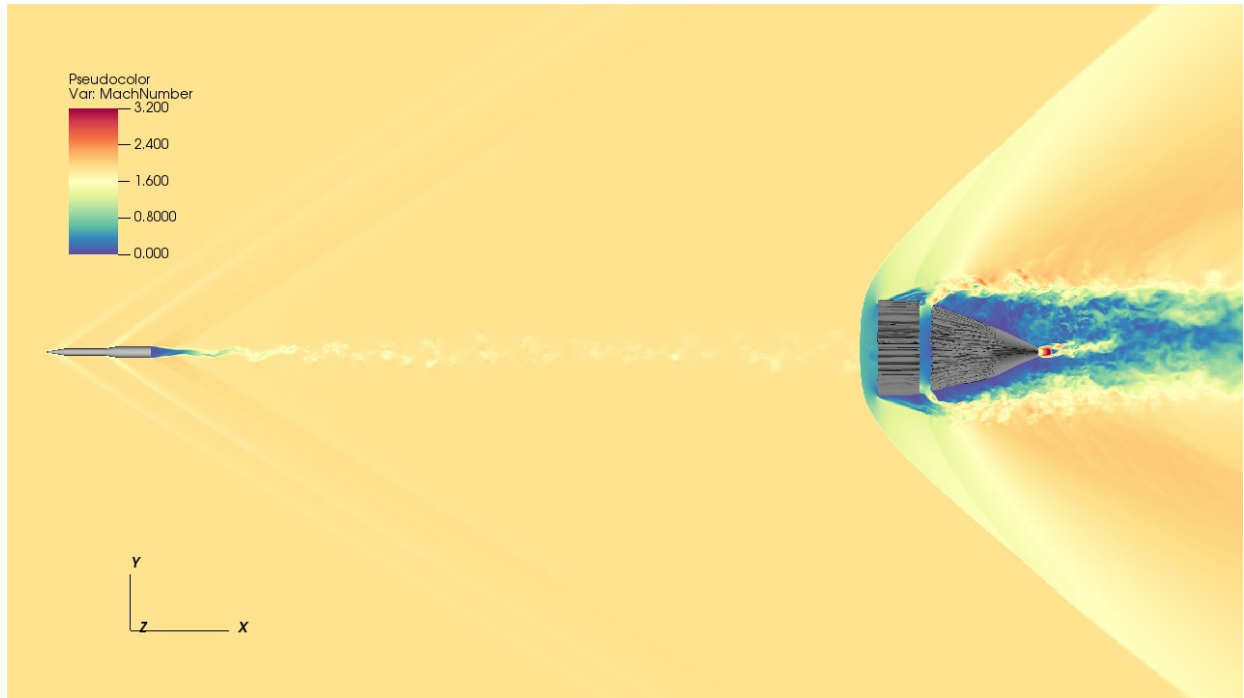
Fig. 9 Axial force coefficient C_A comparison between ASPIRE measurements and LAVA FSI simulations with and without viscous effects.

per time-step and is currently restricted to first order accuracy. The additional cost could be alleviated by leveraging an arbitrary Lagrangian-Eulerian (ALE) formulation where the grid moves with the rigid body (payload) instead of the payload moving with respect to the grid. We take another approach here where we switch from the third order explicit SSPRK scheme to the second order SSPRK scheme, which has one fewer stages, increasing performance by 30%. To better understand the effect of acceleration on the parachute drag, we perform a simulation with identical inputs to those in Section IV.D with the exception that 6-degrees-of-freedom deceleration of the payload is modeled. Additionally, the domain's extent in the x-direction is made larger by a factor of two (adding length downstream of the vehicle) to accommodate the large displacement of the system. The fixed mesh refinement regions from the previous non-decelerating simulation were moved in the axial direction to follow the payload's movement downstream to provide as close a comparison as possible without solution-based AMR as illustrated in Fig 13.

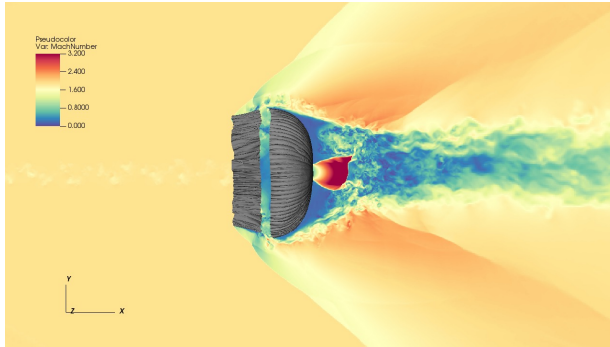
This will compare what is termed an infinite mass ratio inflation to a finite mass ratio inflation. The mass ratio M_R is defined as the ratio of inertial to drag forces expressed in terms of the system mass over the mass of gas (air) ingested by the parachute at full inflation $M_R = m_{\text{total}} / (\rho_{\infty} (S_0 C_D)^{\frac{3}{2}})$. Previous simulations and wind tunnel tests correspond to infinite mass ratio inflation because the payload cannot decelerate due to drag, so the inertial forces are effectively infinite and $M_R \rightarrow \infty$. This is often deemed a reasonable approximation for Martian parachutes – although slightly conservative – due to the relatively large mass of the payload and the very small density of the Mars atmosphere. Conversely, low-altitude Earth parachutes typically fall in the category of mass ratio going to zero $M_R \rightarrow 0$, signifying that the payload decelerates to terminal velocity within the time the parachute takes to inflate fully.

Modeling the system's deceleration has virtually no impact on the time it takes to reach the inflation peak when starting from the same initial condition as the FSI simulation described in Section IV.D, or the path taken to that inflation peak in terms of the shape or position of the parachute – see Fig 14. The most obvious difference from Fig 8 is that the parachute has moved downstream with respect to the fixed frame of reference from which the snapshots are taken. This is especially salient at the post-inflation rebound peak by which time the parachute has translated several meters downstream in Fig 14e compared to Fig 8e. A more subtle difference is that the distance from the band to the bow shock upstream has increased at both the post-inflation trough and more visibly at the rebound peak.

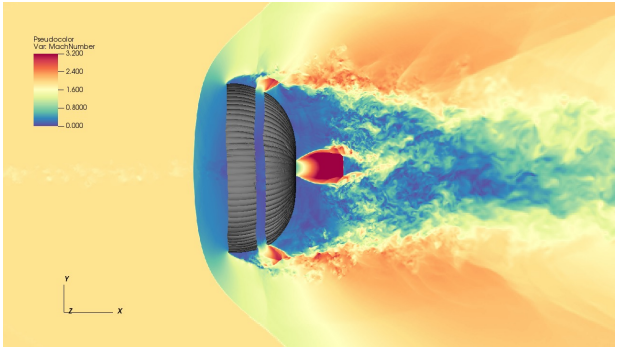
The comparison for the axial force coefficient in Fig 15 shows a 9% drop in the inflation peak, and a downward trend after the rebound peak. This 9% drop in peak parachute force falls within the expected range of opening force factors for $R_M = 1/M_R \approx 0.017$ of just below 0.6 to 0.95 (see Fig 5-50 in [12]) which relates the peak parachute force of infinite mass inflation to finite mass inflation: $C_K = F_{\text{chute}}(R_M = 0.017)/F_{\text{chute}}(R_M = 0)$. The predicted aerodynamic drag matches the estimated flight F_{chute} very well for the rebound peak value, although 0.05 s earlier, and for the deceleration phase for $t > 0.6$ s. The FSI predicted peak drag is still 37.5% higher than the one derived from the measurements of the load pins and accelerometer on board the flight test payload. Fig 16 shows the difference between F_{chute} , and



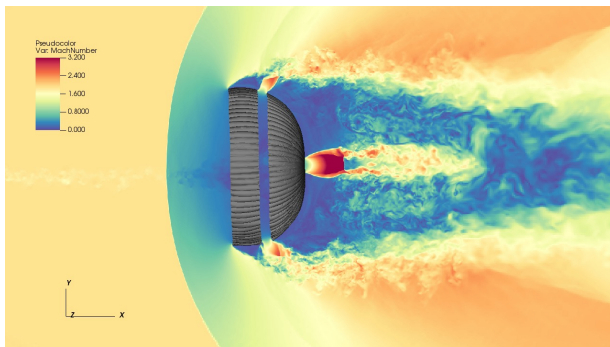
(a) Initial condition for FSI



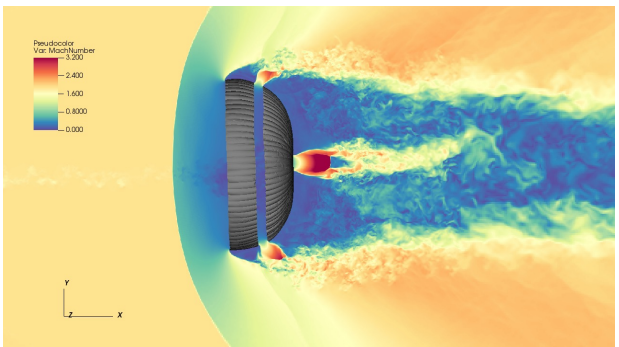
(b) Inflection point in drag curve



(c) Inflation peak



(d) Post-inflation trough



(e) Post-inflation rebound peak

Fig. 10 Snapshots from a simulation with the added mass of the confluence fitting at the bridle to riser junction, viscous effects, and gore seams modeled as stiffer radials. Each shows Mach number on a cut plane, as well as the geometry of the payload and parachute in gray.

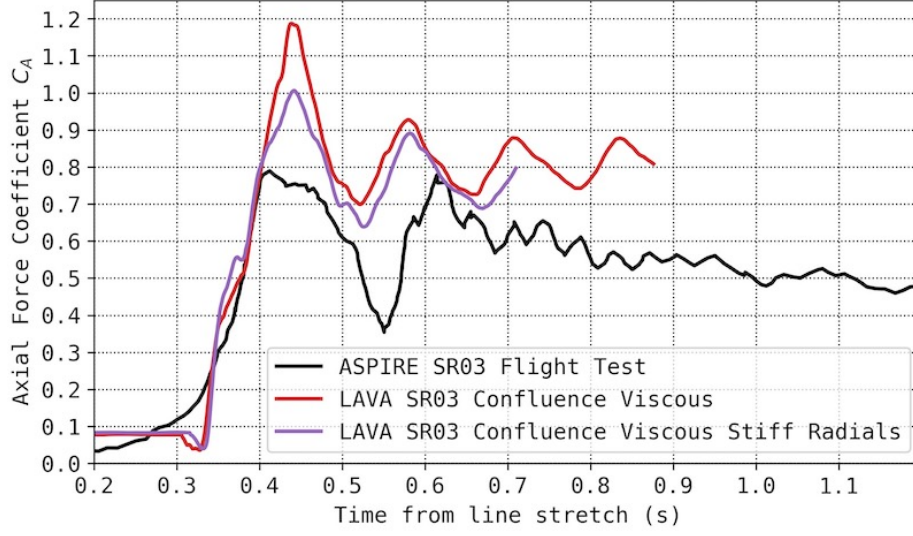


Fig. 11 Axial force coefficient C_A comparison between ASPIRE measurements and LAVA FSI simulations with the confluence fitting, viscous effects, with and without stiff radials.

F_{aero} in the FSI. We compute $F_{\text{chute}} = -(m_c + m_p)a_p - F_{p,\text{aero}}$, where m_p is the mass of the payload, and $F_{p,\text{aero}}$ is the aerodynamic drag of the payload. The sensed acceleration of the payload a_p is computed by taking the derivative of the payload's axial velocity versus time in the decelerating simulations. As expected, the FSI predicted F_{chute} looks like the predicted aerodynamic drag F_{aero} with oscillations associated with the suspension line mode super-imposed due to tension waves propagating through the structural system. The ASPIRE SR02 data is also plotted to show that the aforementioned oscillations started just after the peak in that flight test and the FSI matches the frequency and phasing of those oscillations from $t = 0.4$ to $t = 0.5$ s. The FSI predicted F_{chute} is in excellent agreement with the flight test data in the stable deceleration phase after $t = 0.7$ s.

G. Effect Of Modeling Gore Seams And Deceleration

We also performed a decelerating FSI simulation with the gore seams modeled as stiffer radials. This provides another opportunity to study the effects of deceleration and of the added stiffness of the gore seams. We leverage solution-based AMR for this simulation to better track the system as it decelerates. The error metric for AMR is based on the small-scale total energy as follows: $R \approx (e - \hat{e}) + \frac{1}{2}(u_i - \hat{u}_i)^2$, where the hat operator indicates the trapezoidal low-pass filter $\hat{f} = \frac{1}{4}f(x - \Delta x) + \frac{1}{2}f(x) + \frac{1}{4}f(x + \Delta x)$, e is the internal energy, and u_i is the i^{th} component of velocity. This formulation is based on the proposed large-eddy simulation (LES) error metric from Toosi and Larsson [13] with the addition of internal energy to account for compressibility. We then scale the error metric with inverse distance from the closest object (e.g. parachute or payload) to avoid refining the bow shock and wake to the end of the domain where it has no impact on the quantities of interest for this problem thanks to supersonic nature of the flow. The scaled error metric is defined as follows: $R_d = R/2^{(d/d_{\text{ref}})}$, where d is the distance to the geometry, and $d_{\text{ref}} = 22.4$ m corresponding to half the distance from the payload to the parachute. The thresholds for refinement and coarsening for each Cartesian mesh refinement level are chosen with respect to the freestream total energy and are defined to be factors of 2 apart to avoid coarsening too aggressively. Mesh adaptation is triggered every 100 steps of the simulation, or approximately 0.001 s corresponding to less than 4% of a convective time unit based on the parachute diameter. This frequency of adaptation ensures that the parachute remains on the finest mesh level during its rapid inflation without increasing the overhead cost of the simulation significantly. The simulation was completed in 48 hours on 40 AMD Rome nodes, each with 128 cores split over two sockets, totalling 2.46×10^5 CPU hours. A snapshot of the adapted CFD mesh is shown in Fig 17. Note how the fine mesh is clustered around the payload wake, the parachute bow shock, and the parachute wake. This closely matches how a CFD expert might design this mesh and looks like an optimized version of our previous fixed refinement zone mesh. One exception is how the mesh in the wake of the payload is coarsened midway between the payload and the parachute. By this point, much of the small-scale total energy in the payload wake

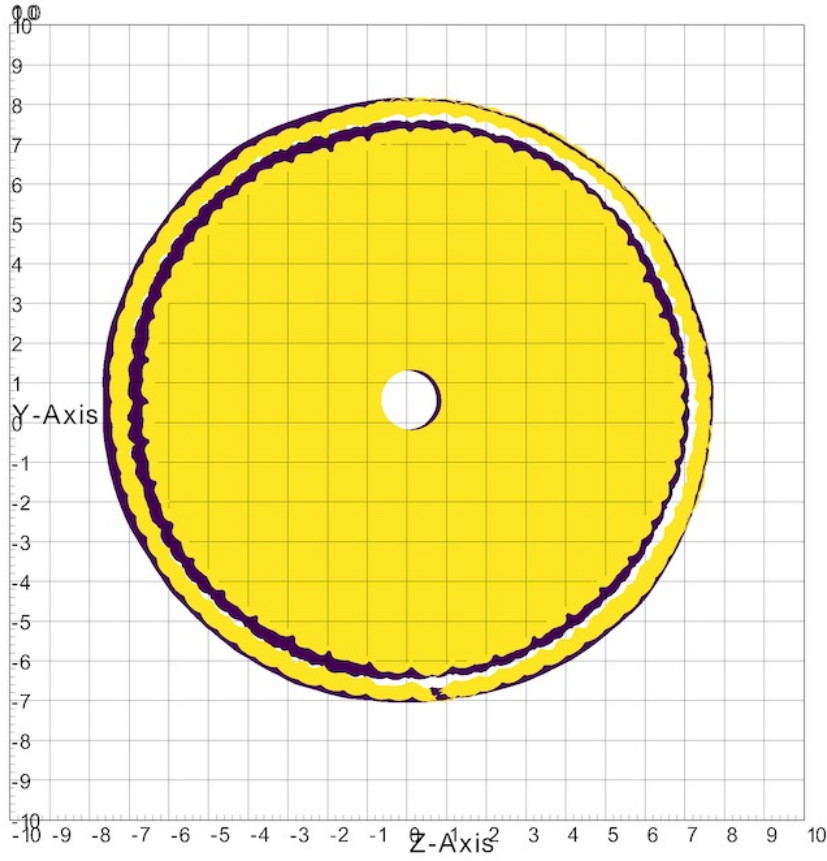
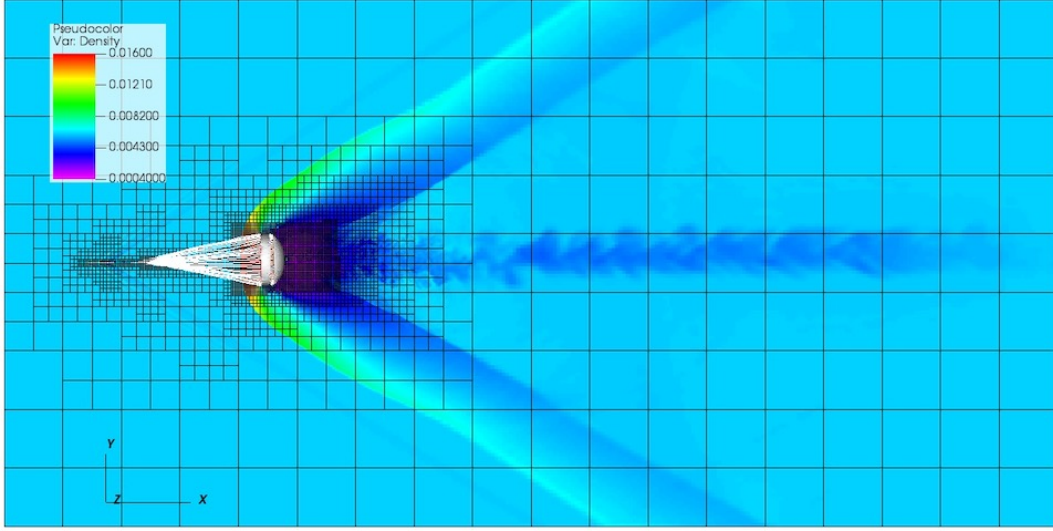


Fig. 12 Comparison of projected canopy area for LAVA FSI simulations with the confluence fitting, viscous effects, with (in yellow) and without stiff radials (in dark purple).

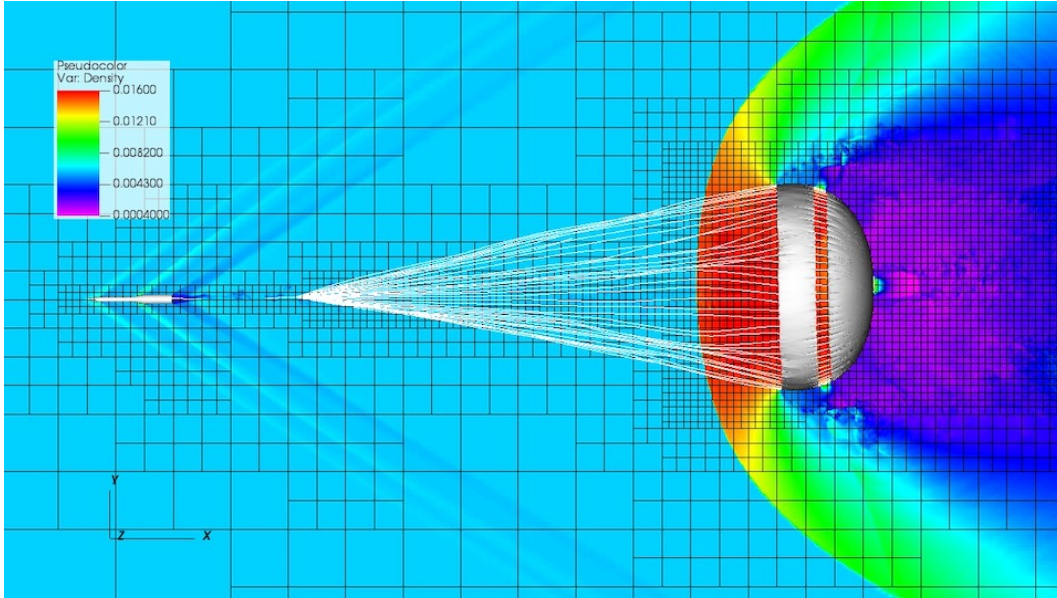
has dissipated. So to match our previous static user-defined mesh which refined the payload wake all the way to the parachute, the error metric threshold value for refinement would have to be dropped significantly. But this would also target the wake of the parachute for refinement over a much longer distance, needlessly increasing the total number cells and the time-to-solution. The coarser mesh is thus a good compromise until a better error metric can be formulated for this class of problems where the wake of one object influences another at a large distance downstream.

Perhaps the most salient change from adding the gore seams modeled as stiffer radials is the shape of parachute as it inflates – curvature in the canopy between each gore seam – and the maximum cross-sectional area. This shape change is visible in Fig 18b to 18e when compared to their counterparts in Fig 14. Fig 19 shows the effect of modeling deceleration when compared to the steadily moving case on the tension at the load pins. Finite mass inflation reduces the peak parachute force by 7% and deepens the post-inflation trough by 14%, bringing each closer to the values measured in flight. The peak drag developed with stiff radials and deceleration compares more favorably with the flight measurements than all previous SR03 simulations as shown in Fig 19. Similarly, the rebound peak value is well-predicted at 8% under and less than 0.04 s early, compared to the larger over-prediction for the case without deceleration. We continue to see an over-prediction of the post-inflation trough drag value despite its drop due to deceleration, which is consistent with our previous simulations.

Fig 21 shows the comparison of the decelerating FSI F_{chute} to that recorded by the ASPIRE SR02 and SR03 flight tests. The comparison to SR02 data is illuminating in that it confirms that the FSI peak aerodynamic drag is not an outlier at 0.91 because SR02 recorded 0.88 during the rebound peak. The FSI-derived F_{chute} displays oscillations in the same frequency and phase as those seen in SR02 from $t = 0.4$ s to $t = 0.55$ s, albeit with larger amplitudes. It recovers a trough value closely in line with that of SR02. This is interesting because it confirms that the trough in F_{chute} is not

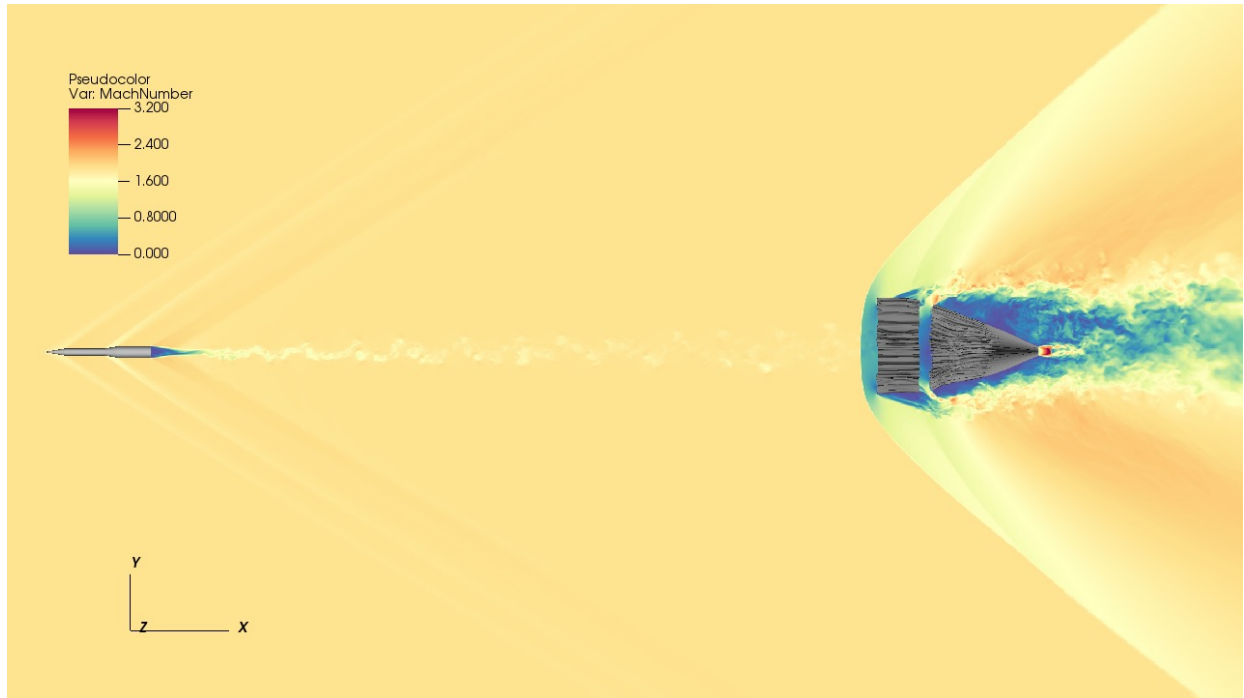


(a) Full domain

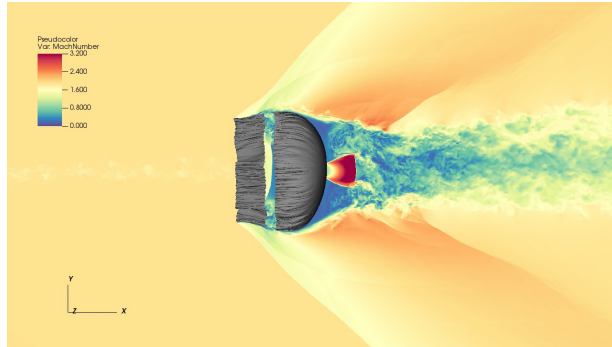


(b) Zoomed-in view

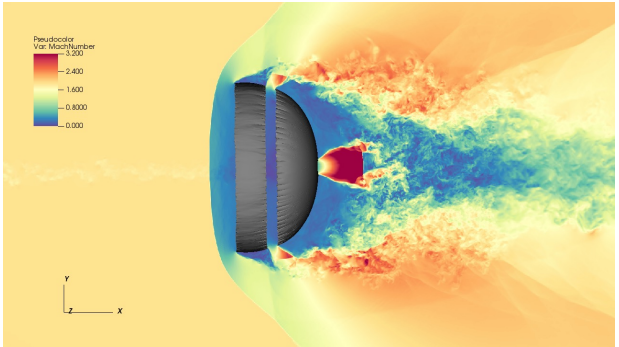
Fig. 13 Slice of density through the CFD mesh for the simulation with the confluence fitting, viscous effects, and free flight (6 DOF deceleration). Boxes of 16^3 cells are shown in black whereas the payload and parachute system geometry shown in white. Snapshot is taken at an instant near the post-inflation trough in drag where the bow shock moves furthest upstream from the parachute.



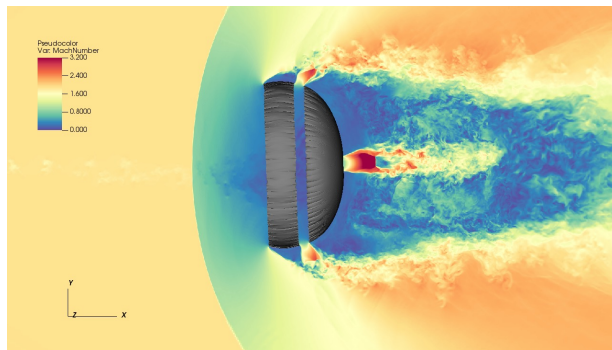
(a) Initial condition for FSI



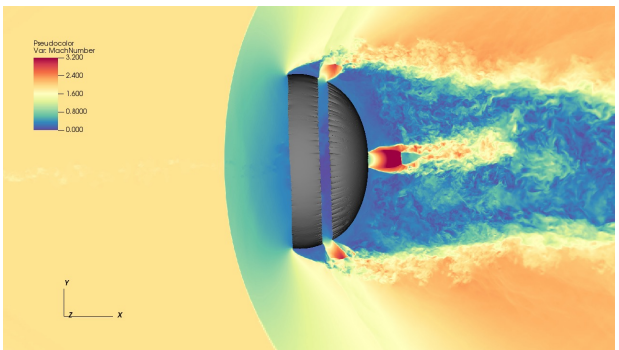
(b) Inflection point in drag curve



(c) Inflation peak



(d) Post-inflation trough



(e) Post-inflation rebound peak

Fig. 14 Snapshots from a simulation with the added mass of the confluence fitting at the bridle to riser junction, viscous effects, and where the payload is decelerating (6 DOF free flight). Each shows Mach number on a cut plane, as well as the geometry of the payload and parachute in gray.

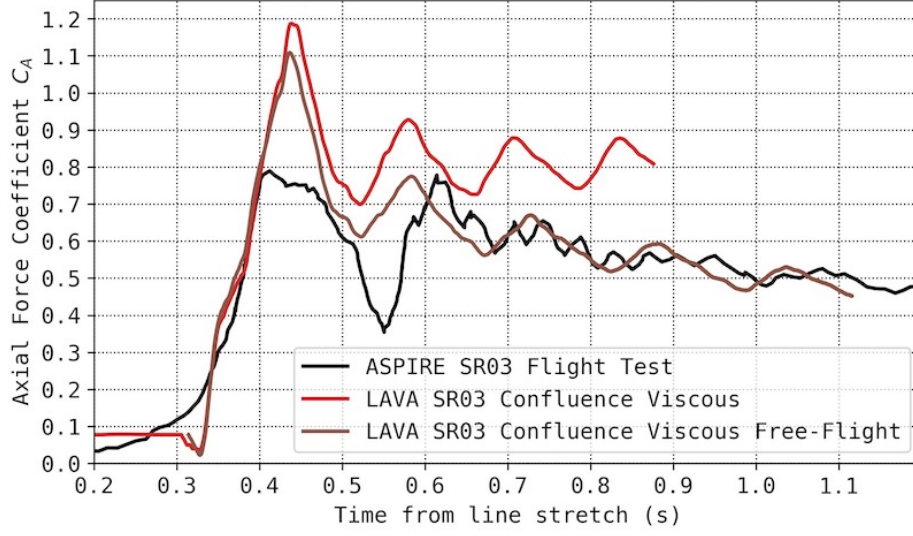


Fig. 15 Axial force coefficient C_A comparison between ASPIRE measurements and LAVA FSI simulations with the confluence fitting, viscous effects, with and without free flight (6 DOF deceleration).

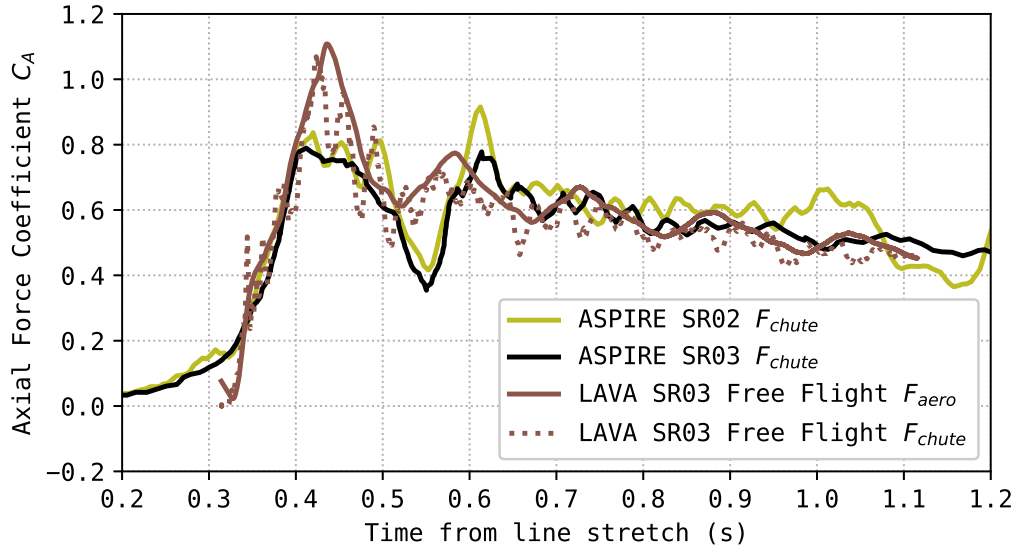
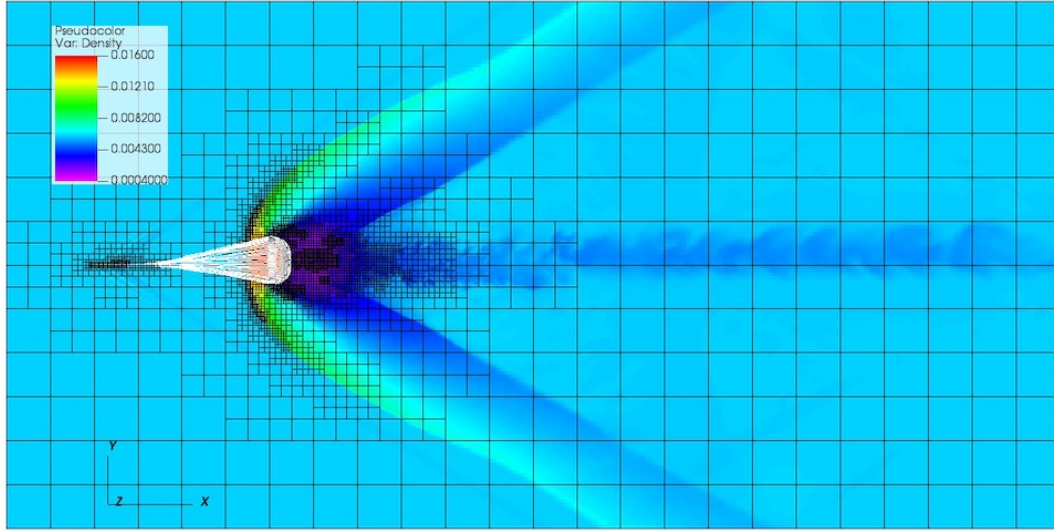


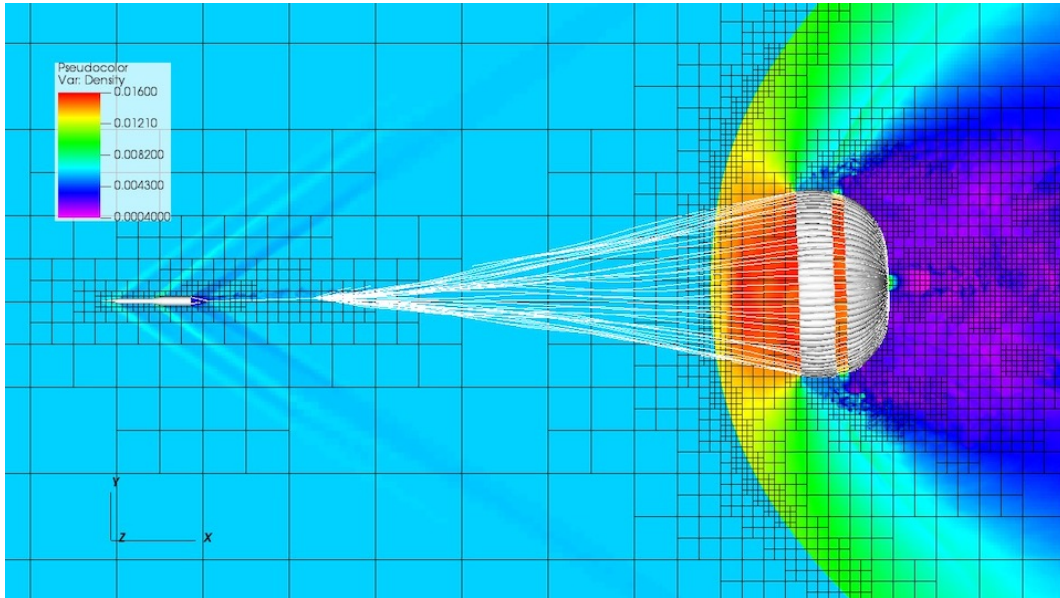
Fig. 16 Comparison of non-dimensional F_{chute} and F_{aero} for LAVA FSI simulation with the confluence fitting, viscous effects, and free flight (6 DOF deceleration) with the ASPIRE SR02 and SR03 F_{chute} measurements.

entirely due to a dip in projected area indicating the partial collapse of the canopy, and is in part due to the structural suspension line mode reducing the tension. These oscillations continue at smaller amplitude in the FSI and both flight tests into the stable deceleration phase, but are not mirrored in the drag – indicating that they are not caused by the lower frequency changes in the projected area of the canopy.

We now review the effect of modeling the gore seams as stiff radials by comparing both simulations with deceleration. Drag shows prominent differences between the two in Fig 20. Namely, the simulation with stiff radials has a local maxima before the inflation peak that corresponds to when the disk is being pulled nearly flat and the bow shock pulled inward into the band at $t = 0.38$ s. It also demonstrates a nearly 20% drop in peak axial force coefficient compared to the simulation without gore seams. The simulation with stiff radials then goes slightly out of phase with a longer second

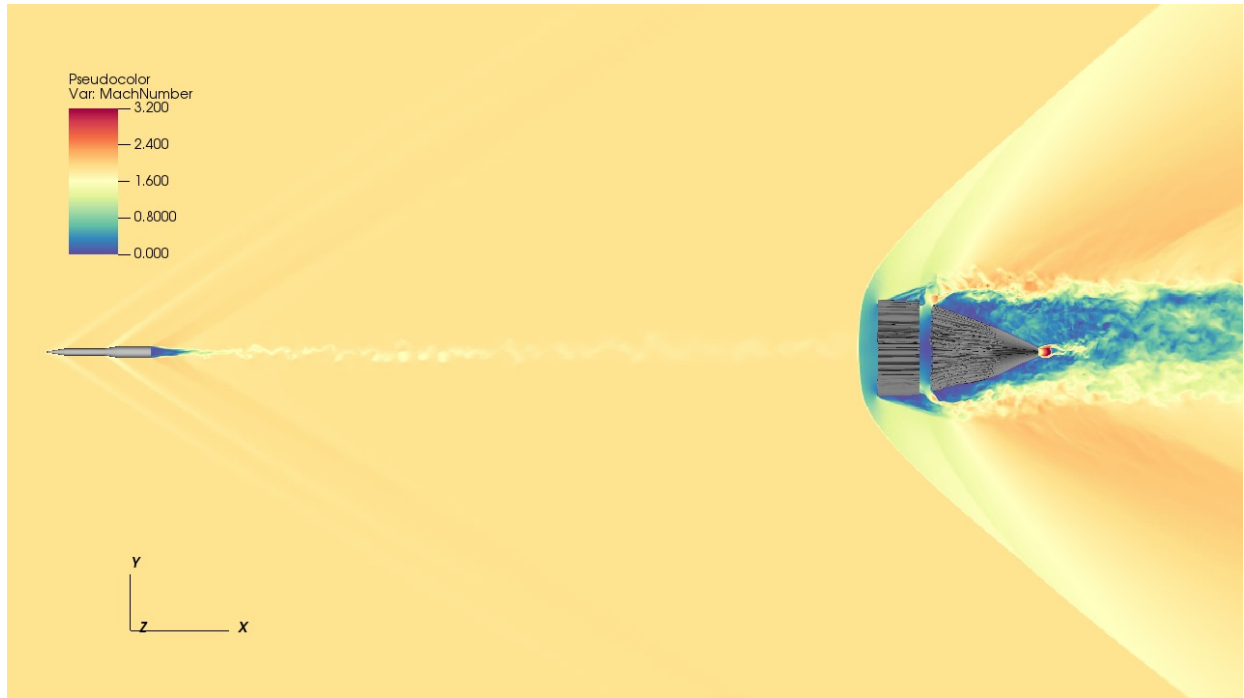


(a) Full domain

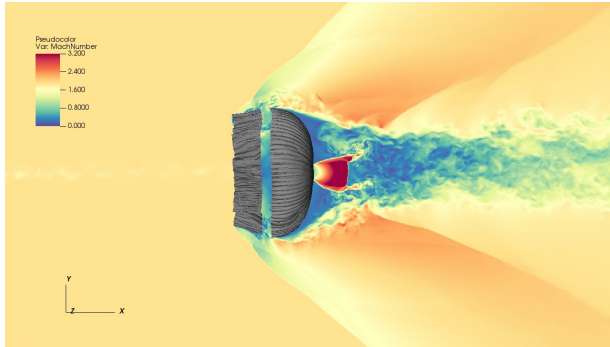


(b) Zoomed-in view

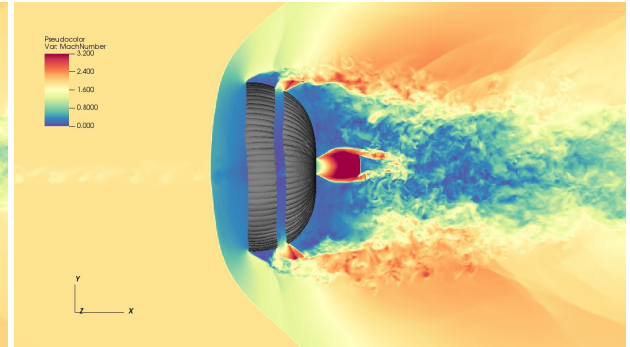
Fig. 17 Slice of density through the CFD mesh for the simulation with the confluence fitting, viscous effects, gore seams modeled as stiff radials, free flight (6 DOF deceleration) and solution-based AMR. Boxes of 12^3 cells are shown in black whereas the payload and parachute system geometry shown in white. Snapshot is taken at an instant near the post-inflation trough in drag where the bow shock moves furthest upstream from the parachute.



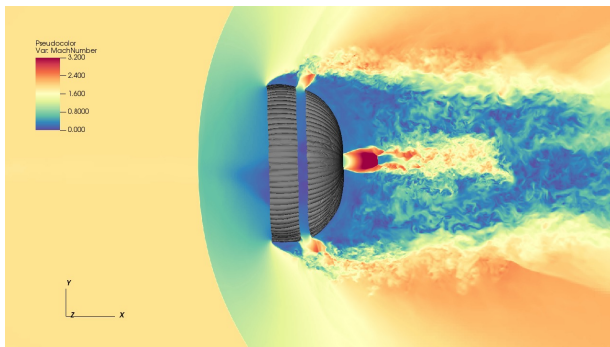
(a) Initial condition for FSI



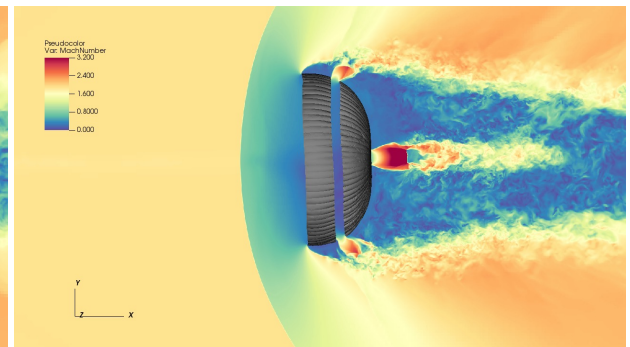
(b) Inflection point in drag curve



(c) Inflation peak



(d) Post-inflation trough



(e) Post-inflation rebound peak

Fig. 18 Snapshots from a simulation with the added mass of the confluence fitting at the bridle to riser junction, viscous effects, gore seams modeled as stiffer radials, and where the payload is decelerating (6 DOF free flight). Each shows Mach number on a cut plane, as well as the geometry of the payload and parachute in gray.

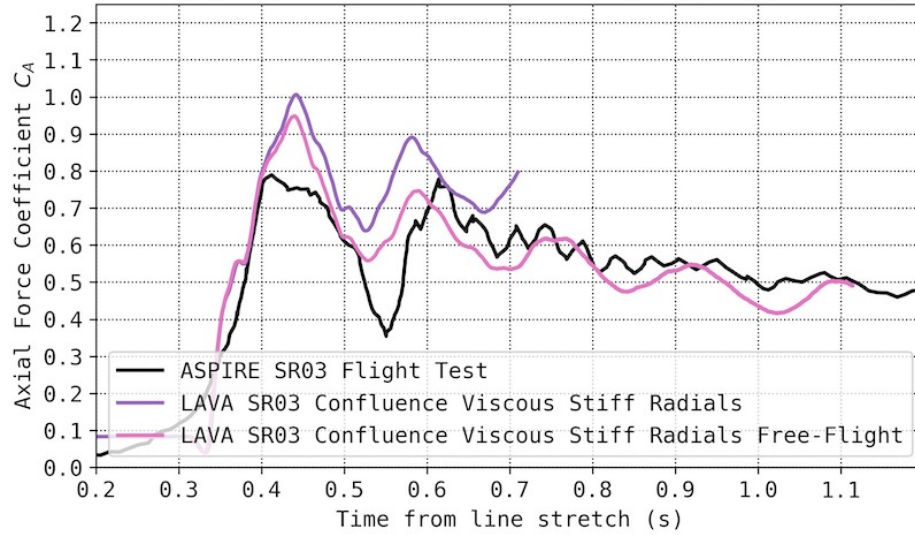


Fig. 19 Axial force coefficient C_A comparison between ASPIRE measurements and LAVA FSI simulations with the confluence fitting, viscous effects, stiff radials, with and without free flight (6 DOF deceleration).

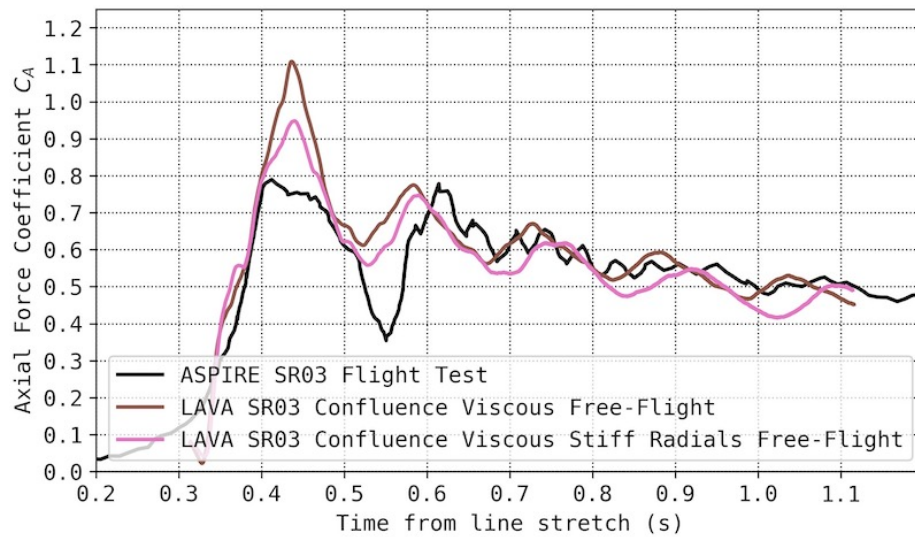


Fig. 20 Axial force coefficient C_A comparison between ASPIRE measurements and LAVA FSI simulations with the confluence fitting, viscous effects, free flight (6 DOF deceleration), with and without stiff radials.

“breathing” cycle for drag. The area changes in those cycles are fairly minimal but drive the bow shock further upstream and back, causing oscillations in the pressurization of the canopy.

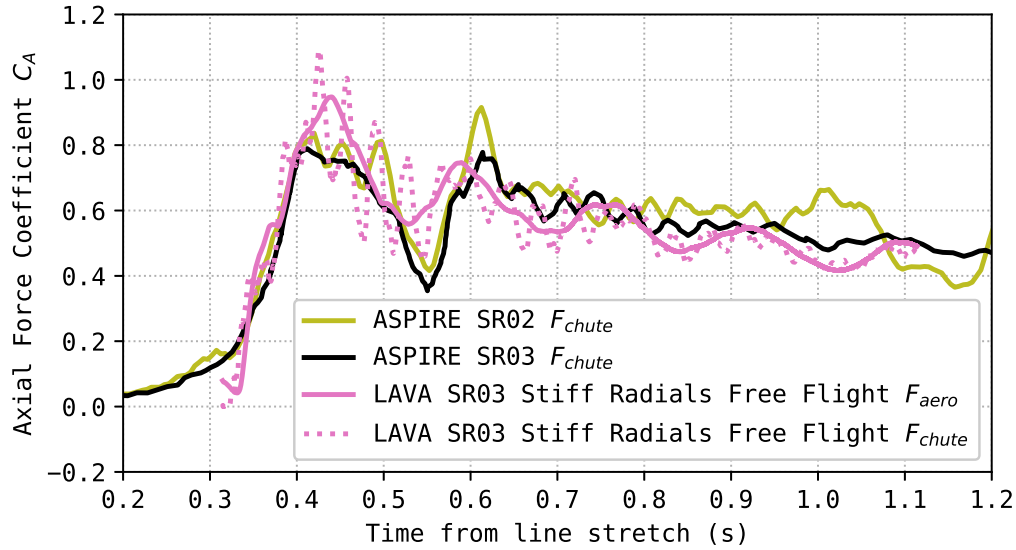


Fig. 21 Comparison of non-dimensional F_{chute} and F_{aero} for LAVA FSI simulation with the confluence fitting, viscous effects, stiff radials and free flight (6 DOF deceleration) with the ASPIRE SR02 and SR03 F_{chute} measurements.

V. Conclusions

A total of six FSI simulations of the ASPIRE SR03 parachute flight test were performed to better understand the sensitivity of the non-dimensional drag coefficient to various modeling choices. Adding the mass of the confluence fitting to the parachute system had a negligible impact on the drag as a whole, but since it comes at no computational cost and better captures the total mass of the system, it will continue to be included in future simulations. Modeling the gore seams as stiffer radials caused a nearly 20% drop in the peak drag by further restricting the total cross-sectional area of the canopy that is open to the flow. Modeling deceleration led to a 10% drop in peak drag. The simulation that approximated the as-flown system the best – with the confluence fitting, viscous effects, gore seams modeled as stiff radials, and deceleration – showed improved agreement with the flight test data. The maximum predicted drag is still 15% larger than what was measured in the SR03 flight, but within 3% of the rebound peak of SR02, meaning that it is not an outlier. Given the high sensitivity of the predicted peak drag to the gores seams’ stiffness, this leads us to believe that we may still be under-estimating the stiffness of the overall parachute system, and thus the maximum projected area the parachute can take.

We plan to refine our initial conditions and start from a more realistic parachute shape to improve our ability to predict the time to inflation and inflation rate. The importance of viscous effects will be further investigated with stress-based wall-modeled large-eddy simulation (WMLES) and refined meshes to better capture any attached turbulent boundary layers that develop on the parachute and the payload. The effect of the vent line foreshortening and fabric “take-up” wherever stitching is used on the disk area will be investigated in the future to quantify its effect on peak parachute drag force.

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